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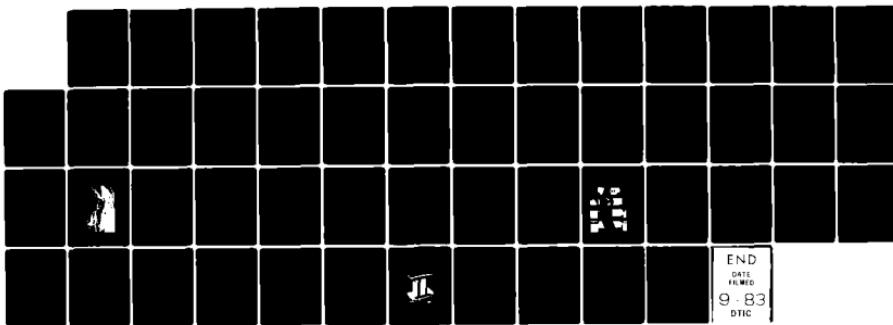
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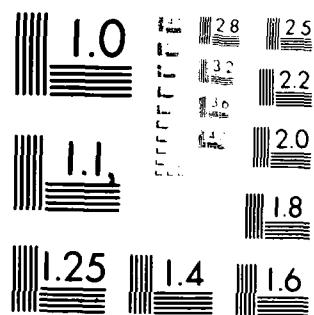
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THE COMPRESSIVE STRENGTH OF CARBON FIBRE REINFORCED PLASTICS

by

K. F. Port

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SUMMARY

The merits of some alternative test specimens for the measurement of compressive strength of unidirectional carbon fibre reinforced plastics are discussed and it is concluded that whilst the RAE design is close to the optimum some improvements are desirable. Tests demonstrated that the present standard RAE specimen is measuring a buckling strength. A modified specimen is suggested which measures a representative compressive strength of the material and this value has been shown to be attainable in the 0° plies of some multi-directional laminates.

It has been shown that compressive failure of current unidirectional material involves fibre microbuckling and that a shear failure is not attainable even at -70°C. The compressive stress-strain response of unidirectional CFRP has been shown to be nonlinear and this is attributed to elastic fibre microinstability which is initiated by initial fibre waviness.

A brief experimental investigation has shown that the compressive strength of multi-directional CFRP (0° ± 45°) does not obey a rule of mixtures when more than 30% 0° plies are present. It is therefore concluded that the compressive strengths of such laminates cannot be calculated from the properties of the constituent plies.

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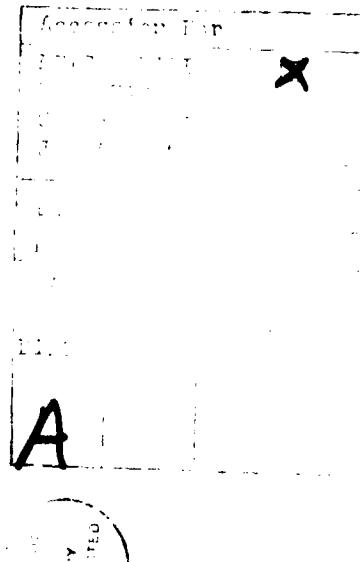
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! INTRODUCTION

One of the limiting factors in a strength-based design using currently available CFRP (carbon fibre reinforced plastics) is the compressive strength of the unidirectional material. In 1973 Ewins and Ham¹ showed that for unidirectional CFRP (HMS and HTS fibres in an ERLA 4617 resin) the compressive strength was approximately 90% of the tensile strength. Furthermore, a microbuckling type of failure mode was only apparent at elevated temperatures (above approximately 100°C); the failure mode under room temperature conditions was one of shear across the fibres and the resin. This transition between a shear and a microbuckling type of failure mode is clearly illustrated in Fig 1 which shows the variation of compressive strength with temperature and is based on the results of Ewins and Ham¹. The transition is shown by a sudden change in the slope of the curve, the compressive strength varying almost linearly with temperature for both failure modes.

In currently used materials, however, this transition is not usually encountered and a microbuckling type of failure is the only one which is normally observed. For example, the measured compressive strength of unidirectional CFRP in a fibre/resin system that is currently widely used (Ciba-Geigy XAS 914) is only about 60% of the tensile strength and the failure mode at room temperature is one involving microbuckling of the fibres. This microbuckling type of failure and the large variation between tensile and compressive strengths of CFRP is, in fact, not peculiar to one particular fibre/resin combination but is exhibited by many current systems which tend to have similar fibre and resin properties. Therefore, throughout this Report the term compressive failure will imply a microbuckling failure mode.

A microbuckling failure surface is shown in Fig 2 where rows of buckled fibres may be clearly seen; the height of the steps is probably a multiple of the buckling half wavelength. The fractographic analysis² of compressive failure surfaces is very difficult, due to a large extent to the post-failure damage resulting from relative movement of the failed surfaces in contact. It must also be realised that a microbuckling failure surface is, of course, only the final result of a compressive failure; its initiation may have been caused by another phenomenon, possibly a macrobuckling or even a shear failure; the latter will, however, later be shown to be unlikely.

The variation in the relationship between the tensile and compressive strengths of unidirectional CFRP has arisen largely by virtue of a continued improvement in the tensile strength of the fibres due to better fibre processing and surface treatment techniques, the tensile strength of the composite being a fibre-dominated property. In contrast, an essential factor in compressive performance is the resin stiffness and so far as CFRP in aerospace applications is concerned, currently used materials tend to employ resins having lower stiffnesses than those used some years ago. One of the main reasons for the resin stiffness not being improved and possibly decreased is that any significant increase tends to produce a more brittle material which, when in composite form, has a detrimental effect on properties such as notch sensitivity and interlaminar shear strength. The dependence of the compressive performance on resin stiffness is

illustrated in Fig 1 where it is evident that the transition to a microbuckling failure is associated with a significant decrease in resin stiffness.

Despite these reasons for the discrepancy between the compressive and tensile strengths of current material being understood there was still some doubt as to the validity of compressive strengths currently being measured. The reason for believing that a true value of the compressive strength of unidirectional CFRP was not being measured was the variation in reported measured values. Different test methods have resulted in room temperature compressive strengths of between 1100 MN/m² and 1800 MN/m² for similar fibre/resin combinations. Furthermore, results from compressive tests on multidirectional CFRP have indicated that the unidirectional plies were exhibiting failure stresses equivalent to 1500 MN/m² (this will be described in section 6). These beliefs are reinforced if the theoretical analyses of compressive failure are considered. The theoretical analysis of a microbuckling failure has been attempted by several authors^{3,4}, who assumed initially straight aligned fibres, and found that the predicted failure stresses were at least 50% above those being measured. However, it is well known that any instability-precipitated failure is highly sensitive to initial imperfections, *i.e* fibre waviness or misalignment, and an analysis including this type of effect predicted far more realistic failure strengths⁵. Nevertheless the value assumed for any initial imperfections must at present be based on informed guess work.

It was the variation in measured compressive strength of unidirectional CFRP that prompted the work that will be reported in this paper, which attempts to determine what is a truly representative and reproducible value and how it should be measured.

The material in all the experimental programmes is a cured, unidirectional carbon fibre composite comprising XAS-914 (Ciba-Geigy) with a nominal fibre volume fraction of 60% and less than 0.5% voids. Unless stated otherwise, all of the values for material properties reported in this document refer to composite that has been stored (for approximately 3 months) and tested under room temperature ambient conditions; therefore the composite would have a mean moisture content of approximately 0.5% in weight.

2 UNIDIRECTIONAL COMPRESSIVE STRENGTH TESTS

2.1 Types of test specimen

There are several test specimens in current use for the measurement of the compressive strength of unidirectional CFRP. They may be broadly categorised into three types depending upon the method of load transmission into the specimen:

- (i) shear loading;
- (ii) direct loading;
- (iii) a combination of shear and direct loading.

The most commonly encountered specimen that employs type (i), pure shear, loading is the ASTM Standard D3410⁶ which is illustrated in Fig 3. The end tabs are commonly made of GFRP (glass fibre reinforced plastics) and the specimen is tested in a fixture which incorporates compressively rated wedge grips that act upon the end tabs, details of the fixture being given in the Standard. This specimen was originally devised by the

Celanese Research Co, USA⁷ and is often referred to as the Celanese specimen. For the specimen shown in Fig 3 the Standard assumes that buckling will not occur.

A modification to ASTM Standard D695⁸, by a Boeing specification in 1979, is an example of type (ii) loading where effectively all of the load is transmitted into the specimen by direct loading onto the ends. This specimen which is illustrated in Fig 4 has a buckling restraint attached to it, details of which are given in the Standard⁸.

The standard RAE specimen, illustrated in Fig 5, represents a type (iii) loading situation with approximately half of the load being transmitted by shear through the adhesive and half by direct loading through the end of the specimen. This is achieved by the use of accurately machined aluminium alloy end fittings and a closely controlled adhesive bond thickness. The specimen was originally devised by Purslow and Collings⁹ in 1972 and has been successfully used for some years within the RAE and the UK aerospace industry.

2.2 Comparison of results

An experimental comparison of these three test specimens has been reported by Woolstencroft¹⁰ and the summarised results are given in Table 1. The results quoted are for XAS 914 material which had been dried until the weight was constant, thus eliminating any moisture within the composite (the presence of moisture reduces the compressive strength of the material). The results clearly show that the modified D695 specimen gave the highest failure stress followed by the RAE and D3410 specimens. However, the C_V (coefficient of variation) for the RAE specimen was the lowest and this is significant because for design purposes it is necessary to take the variability into account. In order to obtain design values it is customary to take the mean failure stress less a certain number of standard deviations (normally three), and on this basis the strength of the RAE specimen is highest at 1312 MN/m^2 , followed by the modified D695 and D3410 specimens at 1168 MN/m^2 and 882 MN/m^2 respectively. Thus it can be seen that it is not necessarily the highest mean value which is of importance; what is required is a high mean value combined with a low variability.

A note of caution must, however, be added because, if the degree of variability in strength can vary between carefully controlled laboratory tests than it is likely that the variability in compressive strength from point to point in a real structure will be quite considerable. The question of the way in which such design data must be applied in practice is, however, beyond the scope of this Report. It is sufficient to note that there is a requirement for a test method which can produce consistently repeatable results that may be used for the collection of design data and for the study of failure mechanisms and degradation processes.

On the basis of the above criterion these results, which were for specimens representing all three loading categories, might be taken to indicate that a specimen which combines shear and direct loading (type (iii)) appears to give the most satisfactory results, ie the RAE specimen. However, it must be noted that of the three the RAE specimen is the only one which employs a waisted gauge section thus reducing the effects of the stress concentration at the end fittings. This concentration may be

precipitating failure in the other specimens examined which could mean that their results represent a lower bound on the strength. This conclusion regarding the optimum type of load transmission into the specimen was also reached by some Russian authors¹¹ in 1979 who had carried out an investigation specifically to determine the optimum test specimen loading configuration. These authors concluded that, for high strength unidirectional composites with low shear strengths, a combination of direct and shear loading enabled higher values for the strength to be obtained with a smaller scatter. These conclusions support the results obtained by Woolstencroft which indicate that the RAE type of specimen approaches the optimum design.

In the previous section it was stated that compressive strength values between 1100 MN/m² and 1800 MN/m² have been reported when using various test methods. This latter high result, which was reported by a German company for Toray/914 material, exhibited a C_V of only 4.0%. (NB: The properties of XAS-914 and Toray/914 are very similar.) The result was obtained using a modification to the ASTM D3410 method by introducing a thicker specimen with a shorter waisted gauge length, as shown in Fig 6, but employing the standard D3410 fixture. The initial reaction to these high values was that because of the very short specimen gauge length a certain amount of Poisson expansion was being restricted and hence a lower energy mode of failure was inhibited. Some specimens of this modified design, but using XAS-914 material, were tested in Materials Department RAE with the gauge length being varied by altering the grip separation in the fixture. The length of the waisted section was, however, not varied. The results¹² from these tests are given in Table 2. It is evident from these results that the high values achieved by the German Company were not reproduced but the value found for the shortest gauge length was considerably higher than previously encountered using other test methods. The results also show that the specimens with free lengths of 8.5 mm and 10.1 mm gave very similar strength values whereas the greatest length of 12.7 mm gave a reduced value and this could indicate an instability effect. Further reference to this phenomenon will be made later in the Report, together with a possible explanation for these values.

3 THE RAE SPECIMEN

It has been shown that the standard RAE method was superior to the other techniques in giving a consistent measure of compressive strength. However, there was a strong belief, in view of the evidence from the modified D3410 unidirectional data and from results on multidirectional material, that a true measure of the compressive strength was not being obtained. As a result of this evidence several standard RAE specimens were tested with back-to-back strain gauges, ie with gauges attached in the centre of both faces of the gauge length. The resulting stress-strain curves indicated that the specimen was not measuring a true compressive strength value but was exhibiting macroinstability. A typical stress-strain plot is shown in Fig 7 where it is clear that the strain curves for each face of the specimen are diverging, indicating the onset of bending and an instability-precipitated failure. When the specimen was originally designed it was confirmed that its configuration was such that compressive failure would occur before any instability precipitated failure; the evidence of Fig 7 indicates therefore that some

degree of change in fibre and resin or interface properties has occurred since the initial design. Further evidence of a macroinstability failure was found by measuring the out-of-plane deflection of a specimen whilst under test. The out-of-plane deflection just prior to failure was measured as 0.4 mm, hence clearly indicating a buckling effect. Furthermore, it was shown by experiment at the time of the original design that the coefficient of constraint (see section 3.1.1) was 3.6 and it will be seen later that this figure may now also be in error.

3.1 Effects of gauge length variation

3.1.1 The slenderness ratio and macrobuckling

The simplest way of preventing the standard RAE specimen exhibiting macrobuckling characteristics was to reduce the gauge length. Therefore, several tests were performed on RAE type specimens with varying gauge lengths or, to adopt a more general term, varying slenderness ratios. Slenderness ratio is defined as l/k where l = free length (gauge length) and k = least radius of gyration of the cross-section.

The simple Euler formula for predicting the critical buckling stress σ_{cr} of a constant section strut of 'free' length l may be written as

$$\sigma_{cr} = \frac{C\pi^2 E}{(l/k)^2} \quad (3-1)$$

where E = longitudinal Young's modulus in compression.

The coefficient of constraint C (see, for example, p 415, Ref 13) nominally varies from 1 for a simply supported condition to 4 for a fully *encastré* condition, but its effective value for most practical situations must be determined by experiment. Also an exact solution for the slenderness ratio of a specimen having a waisted gauge length does not exist. As a conservative approximation the slenderness ratio for a constant rectangular section gauge length of thickness d_0 (minimum waisted thickness) and length l (see Fig 9) may be used.

The slenderness ratio for such a section may be written as

$$\frac{l}{k} = (\sqrt{12}) \frac{l}{d_0} \quad (3-2)$$

The range of specimens tested had slenderness ratios varying between 25 and 65, the value for a standard RAE specimen with an 18 mm gauge length being 46.2. All of the specimens were waisted in thickness from a basic 2.0 mm to 1.35 mm with the slenderness ratio being varied by changing the gauge length from a minimum of 7.0 mm to a maximum of 25.0 mm. In order to ensure that the waisted region extended over the complete gauge length (*i.e.* there was no parallel section) different waisting radii had to be used for each value of slenderness ratio.

The results of these compressive tests are given in Table 3 and a plot of failure stress against slenderness ratio is shown in Fig 8. It is evident from Fig 8 that

decreasing the slenderness ratio below that of the standard RAE specimen results in an increase in the compressive strength until a maximum is reached at a ratio between 10 and 13. If the ratio was further decreased, as will be more fully described in section 3.1.2, there was a change in the failure mode with interlaminar shear failure from the radius becoming progressively more evident; this resulted in a decrease in the compressive stress. The data for a slenderness ratio of 11.5 were obtained later (see section 3.1.2).

The experimental results illustrated in Fig 8 show a sensibly constant mean failure stress for both the 10.0 mm and 13.0 mm gauge lengths but the mean result for the 7.0 mm gauge length is clearly lower. However, this initially anomalous result may be explained if the taper contour equation is examined; this equation may be written as⁹:

$$y = \frac{d_0}{2} \exp\left(\frac{\tau_m^{2x}}{\sigma_m d_0}\right) \quad (3-3)$$

with the relevant dimensions and coordinates shown in Fig 9

where σ_m = ultimate compressive stress,
 τ_m = ultimate interlaminar shear stress,
and D = basic specimen thickness.

This equation governs the allowable rate of change in the cross section of a waisted specimen such that failure should occur by compression and not by interlaminar shear due to an inadequate transfer length. The length of the specimen may be obtained from equation (3-3) as:

$$l = \ln\left(\frac{D}{d_0}\right) \frac{\sigma_m}{\tau_m} d_0 \quad (3-4)$$

For the material used during this investigation, XAS 914,

$$\sigma_m \approx 1500 \text{ MN/m}^2 \quad (\text{taken from Fig 8})$$

$$\tau_m \approx 115 \text{ MN/m}^2 \quad (\text{from previous experimental data}).$$

Hence $\sigma_m/\tau_m \approx 13$ which could be taken to represent a lower bound if high values for compressive strength were attainable. For the specimens under consideration

$$d_0 = 1.35 \text{ mm} \quad \text{and} \quad D = 2.0 \text{ mm}$$

giving

$$l \geq 5.13 d_0$$

or

$$l \geq 6.92 \text{ mm}.$$

Therefore in order for the failure to be compressive rather than interlaminar shear the gauge length of the specimen should not be less than about 7.0 mm. In fact five out

of the six specimens tested with a 7.0 mm gauge length did indeed fail by interlaminar shear and this explains the rather low results for this configuration. The results thus not only demonstrate the validity of the taper contour equation but also support the assumption that the compressive strength has a value of at least 1500 MN/m^2 .

An Euler curve (equation (3-1)), based on simply supported end conditions and calculated using the slenderness ratios defined by equation (3-2), is superimposed on the experimental results in Fig 8. For those specimens that failed in a buckling mode the difference between the experimentally determined failure stress and the Euler value may be attributed to two factors:

- (i) the specimen was not simply supported so that the true coefficient of constraint must have been greater than 1.0;
- (ii) the true slenderness ratio of the radiused specimen must be less than that defined in equation (3-2).

Both these factors would be expected to result in a higher buckling failure stress than the Euler curve value but their relative contributions are unknown; the value of C (equation (3-1)), deduced from the difference between the experimental results and the Euler curve value embraces both these factors. Thus, for the standard 18mm specimen for example, a combined factor of $C = 2.2$ emerges. While it is not possible to separate the different contributions, of end constraint and specimen geometry, to the C values obtained, it can, however, be stated categorically that the value of $C = 3.6$ that was assumed for the coefficient of constraint during the original design of the standard specimen must clearly be in error; a value no greater than 2.2 should be used. This is, perhaps, compatible with the fact that in real structures it is very rare for a C value greater than 2.0 to be realised and this should be taken as the maximum figure when designing for *encastré* conditions.

3.1.2 Failure modes

Four distinct overall failure modes were evident with these specimens and each mode encountered is illustrated in Fig 10. They may be categorised as follows:

- Type A Failure within the gauge length at approximately the point of minimum cross-section, Fig 10a.
- Type B Failure at the section where the specimen enters the end fitting (end fitting failure), Fig 10b.
- Type C Interlaminar shear failure starting at approximately the point of minimum thickness and extending into the end fitting. This results in a premature bearing type of failure at the end of the CFRP specimen, within the end fitting, caused by all of the load then having to be transferred by direct loading on the end of the specimen. Since the loaded material now has no in-plane support, splitting will also occur.
- Type D A mixed mode type of failure where considerable interlaminar splitting is evident. The point of failure tends to be in the region of the minimum

section. On close examination the failure surface for this mode exhibits a combination of compressive and flexural failure.

A Type A failure is normally taken to be a representative compressive failure. It was observed that the failure surfaces were in general at an angle of about 75° to the fibre direction, although it is not clear at present whether or not this is significant. The Type B failure is often referred to as a lower bound value for a compressive failure because it is assumed to have been initiated by the stress concentration effect where the specimen meets the end fitting but is still a valid failure. A Type C failure is invalid for compressive performance since it represents a failure beyond the limits of the specimen, with the actual failure occurring outside the gauge length, *i.e.* the specimen has not been correctly designed. The Type D failure is associated with specimens having higher values of slenderness ratio in which macroinstability is more prevalent, leading to a mixed mode failure. Therefore although the failure is within the gauge length it must only be regarded as a lower bound and would not normally be found with a correctly designed specimen.

The percentage of each failure type that occurred for the various gauge lengths tested are given in Table 4. If these results are examined it is evident that a degree of correlation exists between coefficient of variation and the percentage of failure types that occur for a given specimen, *i.e.* if for a given set of results (from identical specimens) there is a high percentage of failures of one type, then a low C_V results. Alternatively if there are two or more failure types present, with none representing more than approximately 75% of the total, then a high C_V results. With this correlation between C_V and the percentage of specimens having a given type of failure it could be presupposed that each of the failure types would represent discrete populations of failure stress values, *i.e.* Type A fails with a higher stress than Type B etc. However, there were insufficient numbers of each type of failure within a given set of results adequately to verify this possibility with a statistical analysis.

If the results of Table 4 are further examined it is evident that for the specimens with a higher failure stress (10mm and 13mm gauge lengths) there is a trend towards a Type B failure, *i.e.* end-fitting failure. Now the ratio of basic thickness to waisted thickness (D/d_0) of 1.5 was designed to overcome the stress concentration effect of the end fitting for a standard RAE specimen with an 18mm gauge length. Thus in the shorter specimens which exhibit higher failure stresses and hence higher shear stress concentrations in the adhesive at the end fittings, a tendency towards a Type B failure is not unexpected. A possible way to revert to a Type A failure would be to employ a larger D/d_0 ratio, whilst maintaining the same value for slenderness ratio; this would however, require a larger basic thickness and hence a change in end fitting design. It is possible, therefore, that as the higher failure stresses of approximately 15000 MN/m^2 are associated with Type B failures they may represent a lower bound and even higher values may be achievable. However, evidence from multidirectional laminates has shown that a value of 1500 MN/m^2 in the unidirectional plies is the maximum attainable without artificial constraints being used.

From the results plotted in Fig 8 (failure stress versus slenderness ratio) and the discussion in section 3.1 it would appear that the use of a specimen with a gauge length of less than 13 mm should give a consistent reproducible value for compressive strength. The gauge length must, of course, be greater than 7 mm to avoid a premature shear failure as was previously explained. However, the C_V of the results for the 10mm gauge length was in fact rather large with three failure types apparent and, when all of the specimens were closely examined, there was evidence of interlaminar splitting from the waisted section although it was not the initiator of failure. There was no sign of similar splitting on the 13mm gauge length specimens and it would appear that a gauge length of between 10 mm and 13 mm would be the optimum configuration to avoid any interlaminar splitting and still measure a valid compressive strength. Failure strengths measured in this region may none the less still represent a lower bound because the dominant failure mode is Type B which may be caused by the stress concentration effects of the end fittings. As noted earlier, however, the possibility of altering the specimen configuration to revert to a Type A failure would involve departing from the standard laminate test plate thickness of 2 mm.

In order to demonstrate that a gauge length between the previously stated limits would be the optimum, eight specimens were manufactured with a gauge length of 11.5 mm ($l/k = 29.5$) and a 50mm waisting radius to ensure no parallel section within the gauge length. The resulting mean compressive strength from these specimens was 1530 MN/m^2 with a C_V of 3% and there was no sign of interlaminar splitting from the waisted section on any of the specimens. Of the eight specimens tested five failed by Type A mode and three by Type B and, if the Type A failures were considered in isolation, a mean failure stress of 1551 MN/m^2 with a C_V of 1.6% was exhibited. The addition of the Type B failures thus reduced the mean value and increased the C_V , lending further validity to the assumption that Type B failures represent a lower bound on the compressive strength.

The stress-strain plot of Fig 7 showed that the standard RAE specimen with an 18mm gauge length was exhibiting macroinstability and it might have been anticipated that the reduced gauge length specimens of 10 mm and 13 mm, which appeared to be measuring a compressive strength rather than a buckling strength, would not exhibit such characteristics. However, experimentally determined plots of stress-strain for 10mm and 13mm gauge lengths still show evidence of the strains on the two faces diverging, as shown in Figs 11 and 12, indicating macroinstability. The explanation for this effect may be that, because of some initial fibre waviness or misalignment, elastic microbuckling of the fibres commences shortly after load is applied and this gradually reduces the stiffness of the composite until a point is reached where it becomes generally unstable and failure is initiated by macroinstability, as shown in Figs 11 and 12. This reduction in composite stiffness, or non-linearity in the stress-strain response, is clearly evident in Fig 13, this plot being obtained from a plain (unwaisted) specimen with a 13mm gauge length. A mean line has been plotted through the points on this Figure but it is evident that the points are starting to diverge at higher stress levels; the failure stress given is, however, unrepresentative because the specimen was unwaisted and thus failed prematurely at the end fitting. Fig 13 clearly shows the nonlinear response with the initial stiffness having a value of 116 GN/m^2

and the tangent modulus at 1.3% strain being approximately 70 GN/m^2 , a reduction of 40%. Therefore, although the gauge length has been significantly reduced, failure still appears to be precipitated by macrobuckling but the failure mode is one of microbuckling. This curve will, however, be used later to interpret local strains in terms of stress and it should be noted that a projection of the mean curve indicates that, even at very high strains (>2%), it is unlikely that the equivalent stress would be much greater than 1600 MN/m^2 . It would appear impossible for stresses of about 1800 MN/m^2 ever to be achieved with such nonlinearity present.

3.2 A stabilised version of the RAE specimen

It has been suggested above that the compressive failure of unidirectional CFRP is precipitated by a macrobuckling of the specimen which is caused by the stiffness of the material being significantly reduced by elastic microbuckling of the fibres. In order to investigate this possibility further a stabilised version of the standard RAE specimen with an 18mm gauge length was employed. This was achieved by filling the volume between the aluminium alloy end fittings, on either side of the CFRP, with an epoxy resin, the specimen being illustrated in Fig 14. The CFRP specimen was thus encapsulated and so effectively stabilised by restricting out of plane displacement and so reducing the likelihood of macrobuckling. In order to determine the properties of the encapsulating resin (Ciba-Geigy MY753-HY956) one specimen was tested with only resin between the end fittings and four strain gauges attached to the surface of the resin. The stress-strain response produced from this test is shown in Fig 15 and the difference between the readings from any of the strain gauges did not exceed 0.06% strain throughout the test. The resin did not actually fail at the end of the curve shown in Fig 15 but the strain was increasing rapidly with little increase in stress. It is evident from Fig 15 that the stress-strain response of the resin is linear to approximately 2% strain with the compressive modulus up to this point being 3.3 GN/m^2 .

Four encapsulated specimens were tested and each of these had four strain gauges attached. Two 90° rosettes were attached back-to-back on the CFRP specimen (prior to encapsulation) and two longitudinal gauges were bonded to the surface of the encapsulating resin; the positions of these gauges are shown in Fig 14. The load-strain response for a typical specimen is shown in Fig 16 where a mean line has been drawn through the points for each gauge. The actual points for the longitudinal CFRP gauges are shown, where it is evident that some divergence of the strains is taking place. However, although this indicates that some bending is taking place it is very much less than was apparent for the unrestrained specimens.

In order to calculate the failure stress of the composite consider first the strain in the encapsulating resin at failure of the composite. Fig 16 shows this to be 2.11% and, using the stress-strain curve of the pure resin (Fig 15), this may be seen to be equivalent to a resin stress of 68.75 MN/m^2 ; from this the composite failure stress may be calculated as 1536 MN/m^2 . Using this method based on the encapsulating resin strain at failure of the composite the mean composite failure stress for all of these specimens was 1504 MN/m^2 with a $C_V = 7.4\%$. It should be noted that in all cases no failure of the

encapsulating resin was apparent. Alternatively it is possible to calculate the composite failure stress from the mean failure strain in the composite, which the gauges showed to be 1.65% strain with $\epsilon_y = 9.7\%$. If this value is used in conjunction with the projected mean stress-strain curve for an unrestrained specimen as in Fig 13 it may be observed that this represents a composite failure stress of approximately 1500 MN/m^2 , just as predicted from the encapsulating resin strains. It should be noted that the failure surface of the specimens still showed evidence of fibre microbuckling.

One major difference between the encapsulated and the normal unrestrained specimens lay in the CFRP transverse failure strain, which for the former was 1% and for the latter only 0.4%. This difference may be explained by the fact that out-of-plane expansion of the CFRP is largely restricted by the encapsulating resin thus increasing the magnitude of the in-plane (transverse) strain. Furthermore, the ratio of Poisson's ratios in the CFRP to that in the encapsulating resin is approximately 1:1.24 so that the encapsulating resin is actually trying to compress the CFRP in the out-of-plane direction and this would also increase the CFRP expansion in the in-plane direction.

These results indicate therefore that, even when macrobuckling is largely restricted, the maximum compressive strength of CFRP is still approximately 1500 MN/m^2 , as previously demonstrated with unrestrained specimens of reduced gauge lengths (see section 3.1 and Fig 8). Now it was stated earlier that the failure of the unrestrained specimens is initiated by macrobuckling, and strain gauge evidence supported this, yet the encapsulated results indicate that a similar failure stress to the unrestrained result is still exhibited even when very much less macrobuckling (or bending) is present. Note, however, that it is the maximum local stress in the gauge section that will initiate failure and, depending on the degree of bending (or secondary applied stress systems), this may differ significantly from the mean applied stress. This will be discussed further in the next section.

4 FAILURE MODES IN COMPRESSION

4.1 Stable and unstable failures

As previously stated in the introduction, the term compressive failure here implies a fibre microbuckling failure mode. This local failure criterion may, however, be achieved with the specimen as a whole in either a stable or an unstable condition. A stable failure is one in which no macrobuckling of the specimen takes place and failure is due to the fibre microbuckling, critical stress being attained over the whole of the specimen cross-section.

An unstable failure is one in which a fairly low level uniform stress field exists until macrobuckling of the specimen occurs at which point a high non-uniform stress is suddenly added and failure occurs. This failure is induced by the critical fibre microbuckling stress being attained in a small part of the section which then propagates across the whole of it. In this case the direct mean stress applied to the specimen can be considerably less than that for a stable failure.

In some specimens a certain degree of bending is evident and, depending upon its magnitude, the failure can be either stable or unstable.

4.2 Initial waviness and the role of microbuckling

4.2.1 Fibre microbuckling and its effect on the longitudinal modulus

The nonlinear stress-strain response of unidirectional CFRP in compression is probably attributable to elastic fibre microinstability which is initiated, as soon as loading commences, by any initial fibre waviness or misalignment. The effect of the nonlinearity is to reduce the effective stiffness of the composite as the stress level increases, and if the stiffness falls sufficiently macrobuckling can occur.

Although there is no direct evidence, support for this model is given by the results of some tests which involved the repeated loading (up to eight times) of standard 18mm RAE type specimens to 80% of their mean failure stress which showed no discernable variation in their nonlinear stress-strain response. Thus if the nonlinearity is caused by fibre microinstability it must be an elastic phenomenon. Furthermore, two specimens were tested until the stress-strain responses measured by gauges on the two faces had actually diverged significantly, and the load was then released to prevent failure occurring although the specimens had actually experienced greater than the expected nominal failure stress. These, still intact, specimens were then sectioned, polished and examined microscopically, and no damage to either fibres or resin could be found. The verification of the presence of initial fibre waviness, necessary for the model, is however very difficult. Indeed, if a polished section of unidirectional composite is examined microscopically such that individual fibres are easily distinguishable no significant fibre waviness can be observed.

The determination of the initial fibre waviness f_0/l is very difficult and is normally educated guesswork. f_0/l is the ratio of the amplitude of initial fibre deflection to the half wavelength of the deflection. A method for determining the initial waviness is described in Ref 14 which involves repeated polishing of a composite section together with detailed microscopic examination and photography. This method is extremely laborious and time consuming and has not been attempted for current CFRP material. However, a value for a boron-epoxy system has been reported to be in the order of 0.001¹⁵, (boron fibres having a diameter (about four times) that of carbon, the measurement of initial waviness is somewhat easier). The smaller diameter carbon fibres are more difficult to handle during the various manufacturing processes and the f_0/l ratio must be at least of a similar value if not larger.

4.2.2 Failure prediction

The prediction of a fibre microbuckling compression failure has been attempted by many authors^{3,4}, the majority of whom assume straight aligned fibres within the matrix. However, this type of analysis invariably results in compressive strength predictions at least 50% greater than those being measured. Any form of instability failure is highly sensitive to initial imperfections, for example initial fibre waviness, and any analysis that considers such effects is clearly more realistic. One such analysis is now considered. Wang⁵ has proposed an analytical model to predict the compressive strength of unidirectional composites which assumes that failure is due to fibre microinstability and that the fibres have an initial waviness (f_0/l) when the composite is in a stress-free

state. The model assumes an initial waviness of sinusoidal form which is clearly idealistic but it is the simplest means of modelling any initial fibre waviness or misalignment. This model also takes into account the nonlinear shear, stress-strain characteristics of unidirectional composites.

In order to demonstrate the effect of initial waviness, Wang's analytical model was used for several values of f_0/i and the results are shown in Fig 17 as a graph of critical microbuckling failure stress against initial waviness. This relationship may now be used to estimate the degree of initial fibre waviness that would correspond to the observed failure stresses. It has been shown that at a representative compressive failure stress of 1500 MN/m^2 the failure is precipitated by macroinstability of the specimen (unstable failure). It is suggested that if failure was due solely to fibre microbuckling (stable failure), strengths in excess of 1600 MN/m^2 could be found (this minimum value is confirmed in the next section). Reference to Fig 17 would then suggest that for this to occur the initial waviness must be in the order of 0.006 (or 1:167). Any initial waviness of this order would be virtually impossible to detect by normal microscopic observation of stress-free specimens.

4.3 The significance of bending in practice

The results of section 3.2 imply that the maximum attainable compressive strength for CFRP is approximately 1500 MN/m^2 even when out-of-plane displacement is restricted and in Section 6 it will be shown that this is also the maximum stress attainable in the 0° (unidirectional) plies of a multi-directional laminate. Therefore, the compressive strengths quoted in section 2 for the modified ASTM D3410 specimen of 1800 MN/m^2 (reported by a German company) would appear to be unrealistically high, it could be that this particular specimen configuration with its very short gauge length and untapered end-plates introduces some compressive lateral stress that delays the onset of microbuckling failure. This suggestion is given some support by the results for modified D3410 specimens obtained by Materials Department, RAE, where the slenderness ratio has been varied; these results are given in Table 2 and are also shown on Fig 8. These results were briefly discussed in section 2 where it was noted that the two smallest values of slenderness ratio specimens, whilst not achieving the compressive strength figures quoted by the German company, still appear larger than those from the modified RAE specimen, whereas the results from the highest slenderness ratio specimen compare very well with those achieved by the RAE type specimens. Therefore, it would appear that between slenderness ratios of 17.5 and 22.0 a point exists where some constraint becomes sufficient to increase the compressive strength.

Now it has been shown that even when failing at a consistent mean strength of 1500 MN/m^2 the RAE type specimen with 10mm and 13mm gauge lengths was still exhibiting macrobuckling and it may be seen that (Figs 11 and 12) the maximum compressive strain on one face of a specimen at failure (ie the sum of the direct and bending strains) is approximately 2%. If this value of 2% is used in conjunction with the projected mean stress-strain response of Fig 13 it is evident that it represents a stress of approximately 1600 MN/m^2 which is similar to the higher two values for the modified D3410 specimens

obtained by Materials Department, RAE. Therefore, although the mean direct failing stress of the RAE type specimens is 1500 MN/m^2 , the maximum stress at failure is approximately 1600 MN/m^2 in some of the outer fibres. This would imply that a value around 1600 MN/m^2 is a true compressive strength when no macrobuckling occurs (i.e. stable failure, section 4.1). From the results obtained there appears to be little doubt that a true measure for the compressive strength of XAS-914 is in the region of $1500-1650 \text{ MN/m}^2$ and this is more clearly evident in the logarithmic plot of compressive strength against the square of slenderness ratio as shown in Fig 18; the points shown represent the mean values with the scatter being shown in Fig 8. This plot of Fig 18 clearly illustrates the transition from the measurement of a buckling strength to a compressive strength.

It was anticipated that encapsulating the standard RAE specimen would produce failure strengths similar to those reported for the modified D3410 specimen but this was clearly not so. Therefore, it would appear that simply restricting the out-of-plane displacement is not sufficient to increase the strength; some lateral stress would also appear to be necessary. This calls into question just how realistic the values obtained using the modified D3410 are and whether they could ever be achieved in a structural laminate.

5 VARIATION OF COMPRESSIVE STRENGTH WITH TEMPERATURE

In 1973¹ it was shown that evidence of fibre microbuckling on a failure surface was only apparent for specimens tested at temperatures in excess of 100°C when the matrix stiffness had fallen sufficiently, and that a shear type of failure mode occurred at temperatures below this value. For current materials, however, it has been shown that the room temperature failure mode is always by microbuckling. In an attempt to precipitate a shear failure mode in these materials it was decided to perform compressive tests at varying temperatures. It was anticipated that at temperatures below 0°C the resin stiffness might increase sufficiently for fibre microinstability not to precipitate failure. Compressive tests at seven temperatures ranging from 120°C to -70°C were, therefore, performed using the standard RAE specimen with an 18mm gauge length. The results of these tests are shown graphically in Fig 19 with the mean values and coefficients of variations given in Table 5. The graph of Fig 19 clearly shows the compressive failure stress increasing with decreasing temperature; however, all of the failures, regardless of temperature, showed signs of fibre microbuckling. Therefore, it must be concluded that even at -70°C the matrix shear stiffness had not increased sufficiently to promote a shear failure. Superimposed upon the curve of Fig 19 is the variation of resin shear modulus with temperature¹⁶ (914 system) and it is clear that the compressive strength of CFRP is strongly dependent on the resin stiffness. Neither curve on Fig 19 shows any marked change of slope that could indicate a transition from one failure mode to another.

In contrast to these results Fig 1 shows the variation of compressive strength with temperature found by Ewins and Ham¹ where a transition from shear failure to a microbuckling failure is very clear. The variation of shear modulus with temperature¹⁶ for the ERLA 4617 resin system is also shown on the graph of Fig 1 where it is evident that a microbuckling type of failure is associated with a marked decrease in resin stiffness.

The comparison between the two systems (XAS-914 and HTS-ERLA 4617) can be more readily observed in Fig 20 where the variation of compressive composite strength and resin shear modulus, for both systems, with temperature is plotted. These plots for compressive strength were achieved by taking a common divisor of the strength at 20°C in XAS-914, and for resin shear modulus a common divisor of the modulus at 20°C in 914. This comparative plot of the two systems clearly shows that the compressive strength of the current XAS-914 system is superior to that of the HTS-ERLA 4617 system despite the microbuckling type of failure mode exhibited by the current system. The main point to emerge from this plot is the relative magnitude of the resin shear moduli, with the ERLA 4617 being significantly stiffer than 914; this may, to a large extent, explain why a shear mode type of failure is unobtainable with the current system. The stiffness of the 914 resin at -40°C (which was the lowest temperature for which data was available) is approximately equal to that of the 4617 resin at 90°C, and this is very close to the observed shear/microbuckling transition for the HTS-ERLA 4617 material. It might thus have been expected that a similar transition would be exhibited by the XAS-914 material at very low temperatures and it seems likely that small differences between the HTS and XAS fibres were sufficient to prevent this happening (see Fig 20).

Now the local stress system at the most highly stressed point in the gauge length consists of the direct compressive stress plus an additional compressive stress due to a degree of bending - or, in the extreme, of macroinstability. The actual failure due to the combined stress system could be by shear but, for the XAS-914 system, proves in practice always to be by microbuckling. Thus the effect of temperature on compressive strength is two-fold.

- (i) It governs the longitudinal modulus of the composite and thus the degree of bending and the tendency for macrobuckling to occur.
- (ii) It governs the shear modulus of the resin and hence controls the final microbuckling failure.

There is in fact a marked similarity between Figs 8 and 19, both of which show trends of increasing failure stress, the former with decreasing slenderness ratio and the latter with decreasing temperature. This is because the effect of decreasing the slenderness ratio affects the degree of bending or macrobuckling in just the same way as does decreasing the temperature. It cannot, of course, have any effect on the microbuckling behaviour, except in the very extreme case of a gauge length that is so short that it approaches the half wavelength of a fibre microbuckle.

The percentage of each type of failure mode found for the various test temperatures is given in Table 5 where failure types A, D and E were the only ones encountered. The Type A failure is the same as previously described but the Type D failure, whilst being similar in appearance to that previously described with interlaminar splitting, did not show any signs of a flexural failure but was still some form of a mixed mode type. Type E failure was not previously encountered and was similar in appearance to Type A but the failure did not occur at the minimum section but at a point between this and the end fitting. It is evident from Table 5 that Type D failures appear to be associated with

temperatures below ambient; they appeared to be of a particularly brittle type and this would be consistent with lower temperatures where the matrix is becoming stiffer and hence more brittle. The post-failure damage associated with all of the specimens tested was particularly severe and this prevented any detailed comment upon the failure surfaces.

Fig 20 shows that the compressive strength of XAS-914 is superior to the HTS-ERLA 4617 system and that the former does have a superior high temperature capability. It is likely that any attempt to produce a stiffer resin which could promote a shear failure would have a detrimental effect upon elevated temperature performance as well as producing a more brittle material. Therefore, with the present resin system it is not possible to promote a shear failure and any attempt to produce a resin that would promote such failure will most likely have detrimental effects on virtually all of the other mechanical properties. Hence a fibre microbuckling failure may have to be accepted as a representative compression failure for CFRP.

6 THE COMPRESSIVE STRENGTH OF MULTI-DIRECTIONAL CFRP

Unidirectional CFRP by itself, in laminate form, is rarely used in a structural situation but is usually combined with plies of varying orientation in a multi-directional laminate. However, it is the 0° plies (taken to be those parallel with the loading axis) in such a laminate that provide the bulk of the strength and from a structural design viewpoint it is necessary to know the failing strength of these plies and how it relates to the strength of a unidirectional laminate on its own.

6.1 Variation of strength with laminate configuration

In order to gain an insight into the efficiency of 0° plies when employed in a multi-directional laminate a short preliminary investigation was undertaken of the stresses carried by the 0° plies at failure of some commonly employed laminate configurations. The compressive strength was measured on several $0^\circ \pm 45^\circ$ laminates in which the percentage of $\pm 45^\circ$ plies varied from 100% to 16.7%, the laminates being 24 ply (approximately 3 mm thick) and having an evenly distributed lay-up. Simple rectangular coupon specimens were used which were 135 mm long and varied in width from 50 mm to 76 mm. Buckling was restrained by the use of an anti-buckling guide as illustrated in Fig 21. The buckling restraint was attached to the specimen using only 'finger-tight' pressure on the retaining bolts.

The results of the tests are illustrated in Fig 22 by a graph of compressive failure stress versus percentage of $\pm 45^\circ$ plies. Consider first the data for the XAS-914 system; a simple rule of mixtures line is superimposed on the graph with the value of 1500 MN/m^2 for 100% 0° (ie unidirectional) material being assumed from the work previously reported in section 3.1. It is evident from Fig 22 that for laminates with less than approximately 30% 0° plies a rule of mixtures applies and the failure stress in these plies may thus be calculated to be approximately 1500 MN/m^2 (assuming strain compatibility and using the stress-strain response from a unidirectional specimen). However, with greater percentages of 0° plies a rule of mixtures clearly no longer applies and the mean failing stresses in the 0° plies for the configurations with 50%, 67% and 83% 0° plies were all significantly reduced (less than 1000 MN/m^2). These results would appear to indicate that when the 0°

plies are supported by an adequate number of $\pm 45^\circ$ plies the full strength of the former may be achieved but when more than 30% 0° plies are used they do not achieve their full load carrying capacity. It was therefore suspected that the failure might be due to ply instability (macrobuckling of the 0° plies). However, a laminate with 50% 0° plies and 50% $\pm 45^\circ$ plies, where the 0° plies were blocked together in the centre with all of the $\pm 45^\circ$ plies on the outside, was also tested and no significant increase in failure stress was detectable. The failure cannot therefore, be due solely to ply instability and further factors such as the possibility of inter ply de-bonding due to high interlaminar stresses may be responsible.

This deviation of XAS-914 from a rule of mixtures was not apparent several years ago and Fig 22 also shows some experimental values¹⁷ for HTS-3501 material which was manufactured before October 1976 when the fibres had the old level of surface treatment. The unidirectional value for HTS-3501 was obtained using the standard RAE specimen and the multi-directional values were obtained using a specimen devised by Collings¹⁸. The difference in surface treatment levels is reflected by the interlaminar shear strength which for current XAS-914 is approximately 115 MN/m^2 but for the old surface treatment level was about 140 MN/m^2 . It is evident from Fig 22 that the compressive strength of HTS-3501 material closely followed a rule of mixtures with the measured unidirectional compressive strength of the material being approximately 1300 MN/m^2 . Several experimental values¹⁷ for XAS-914 (but with the fibres having the current level of surface treatment) are also shown in Fig 22 and it is evident that these values represent an intermediate curve between the HTS-3501 and the XAS-914 but has a similar form to the latter. Therefore, a major difference between a material which obeys a rule of mixtures and one which does not appear to be associated with the ratio of unidirectional compressive strength to interlaminar shear strength. These ratios are approximately; for HTS-3501 9.3:1, for XAS-914 13:1 and HTS-914 13:1. It is therefore reasonable to postulate that materials such as XAS-914 that have a high ratio may, in certain laminate configurations, exhibit premature interlaminar shear failure before the 0° plies are carrying their maximum load.

The suggestion that some laminate configurations fail prematurely is enhanced further if the compressive moduli and stress-strain responses of various configurations are examined. Both the initial and 1% secant compressive moduli follow a rule of mixtures as illustrated by Fig 23. The stress-strain responses for various laminate configuration is illustrated in Fig 24 where it is evident that the 50% 0° 50% $\pm 45^\circ$ configuration exhibits the lowest failure strain of about 0.98%. Assuming strain compatibility, this represents a failure stress in the 0° plies of approximately 1000 MN/m^2 . This indicates an inefficient use of the 0° plies in as much that failure is not due to the ultimate strength of the unidirectional plies being reached. Such a failure may therefore be regarded as premature because, as well as failing at a low stress, the strain at failure is significantly below that for configurations which obey a rule of mixtures.

This preliminary investigation has, however, shown that in certain laminate configurations the failure stress in the 0° plies is approximately 1500 MN/m^2 and this is directly comparable with the values measured on unidirectional material.

6.2 The width effect

The experimental results for XAS-914 shown in Fig 22 are the mean values obtained from specimens with widths varying from 50 mm to 76 mm. For laminates with 16.7% 0° plies or less, the variation of specimen width had no effect on the failure stress values. However, for all other laminate configurations tested there was a definite width effect, with the results for each width representing discrete strength populations. The effect was such that the wider specimens exhibited lower failing stresses with the worst effect being a reduction in strength of 12.9% for the 76mm wide specimen compared with the 50mm width 50% 0° configuration. It is possible that this effect of strength variation with width may simply be due to non-uniform load input.

This size effect will require further detailed investigation which may also help determine why the experimental results do not conform to a rule of mixtures and may perhaps show that this non-conformity is a size effect. Until this has been established, however, this premature failure must be taken as representative of a practical situation. Thus, for the present, in order to design for multi-directional material in compression it will be necessary to test each laminate configuration to determine the stress level that can be carried. The usefulness of unidirectional compression strength data for design purposes must therefore remain doubtful, although it does give an indication of the properties that may potentially be exploited.

7 CONCLUSIONS

- (1) It has been shown that the standard RAE specimen, with a thickness-waisted gauge section and a combination of shear or direct load input, approaches the optimum design for the measurement of unidirectional compressive strength. The specimen has been shown to produce a high mean value combined with a low variability.
- (2) Subsequent investigations showed that the standard RAE specimen was measuring a buckling strength and it was shown that by reducing the gauge length a higher strength could be obtained. A modified RAE-type specimen is recommended which measures a consistent value for the compressive strength of current unidirectional XAS-914 material (1500 MN/m^2).
- (3) The compressive stress-strain response of current unidirectional XAS-914 CFRP is nonlinear and it is suggested that this is due to elastic fibre microinstability which is initiated by initial fibre waviness or misalignment and hence commences as soon as load is applied. The effect of the nonlinearity is to reduce the effective stiffness of the material to the point where macroinstability initiates material failure. This suggestion is supported by considerable evidence both direct and indirect.
- (4) It has been shown that even when tested at -70°C current materials (eg XAS-914) do not exhibit a shear failure mode and that in the past when such a mode was exhibited the resin shear modulus was considerably higher than that of the current systems. It is concluded that fibre micro-buckling must be accepted as a representative compression failure for current CFRP. Furthermore, any attempt to promote a shear failure by increasing the resin stiffness will have a detrimental effect on most other properties, particularly at elevated temperature.

(5) In all of the unrestrained unidirectional compression tests the failures were unstable, i.e precipitated by some form of macrobuckling. In order to promote a stable failure the out-of-plane displacement of the standard RAE specimen was restrained (hence largely restricting macrobuckling). The measured failure stresses from these specimens showed no improvement over the values found from the unrestrained specimens. This result calls into question the validity of results reported for some other types of test specimens, which were unrestrained, but yielded failure stresses much greater than any measured using the RAE type specimens (restrained or unrestrained).

(6) A brief investigation has demonstrated that for certain $0^\circ \pm 45^\circ$ multi-directional laminate configurations the failure stress in the 0° plies is approximately 1500 MN/m^2 . However, it has also been shown that some configurations fail prematurely with the full efficiency of the 0° plies not being achieved. The results also showed that a specimen width effect was apparent and this requires further investigation as do the premature failures.

Table 1

A COMPARISON OF THREE COMPRESSIVE TEST METHODS FOR UNIDIRECTIONAL CFRP

Test method	RAE	D3410	Modified D695
Mean stress (MN/m ²)	1400	1266	1440
C _V (%)	2.1	10.1	6.3

Stress values are for material which has been dried to a constant weight

Table 2

MODIFIED D3410 COMPRESSIVE STRENGTH TEST RESULTS

Free length (mm)	8.5	10.1	12.7
Mean stress (MN/m ²)	1661	1660	1508
Coefficient of variation	4.7%	3.4%	4.1%
i/k	14.7	17.5	22

Table 3

RESULTS OF COMPRESSIVE TEST ON UNIDIRECTIONAL CFRP SPECIMENS OF VARYING SLENDERNESS RATIOS

Gauge length (mm)	Waisting radius (mm)	i/k	Failure stress (mean) (MN/m ²)	C _V (%)	n
7.0	20	18.0	1436	2.0	6
10.0	40	25.7	1498	5.1	12
13.0	65	33.4	1485	2.4	12
15.0	90	38.5	1375	5.0	11
18.0	125	46.2	1269	3.0	5
21.0	170	53.9	1147	5.2	5
25.0	240	64.2	928	4.6	5

Table 4

DISTRIBUTION OF FAILURE MODES FOR EACH TYPE OF SPECIMEN

Gauge length (mm)	C _V (%)	% failure types				n
		A	B	C	D	
7.0	2.0	-	16.7	83.3	-	6
10.0	5.1	25.0	66.7	8.3	-	12
13.0	2.4	8.3	91.7	-	-	12
15.0	5.0	63.6	36.4	-	-	11
18.0	3.0	100.0	-	-	-	5
21.0	5.2	60.0	-	-	40	5
25.0	4.6	60.0	-	-	40	5

C_V = coefficient of variation

n = number of specimens

Table 5

UNIDIRECTIONAL COMPRESSIVE TEST RESULTS FOR VARIOUS TEMPERATURES

Test temperature (°C)	Mean stress (MN/m ²)	C _V (%)	% failure types		
			A	D	E
-70	1349	5.2	40	60	-
-40	1324	5.6	60	40	-
-20	1275	6.8	60	40	-
0	1179	5.1	60	40	-
20	1159	2.2	100	-	-
70	989	2.8	100	-	-
120	962	9.6	75	-	25

C_V = coefficient of variation

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Fig 1

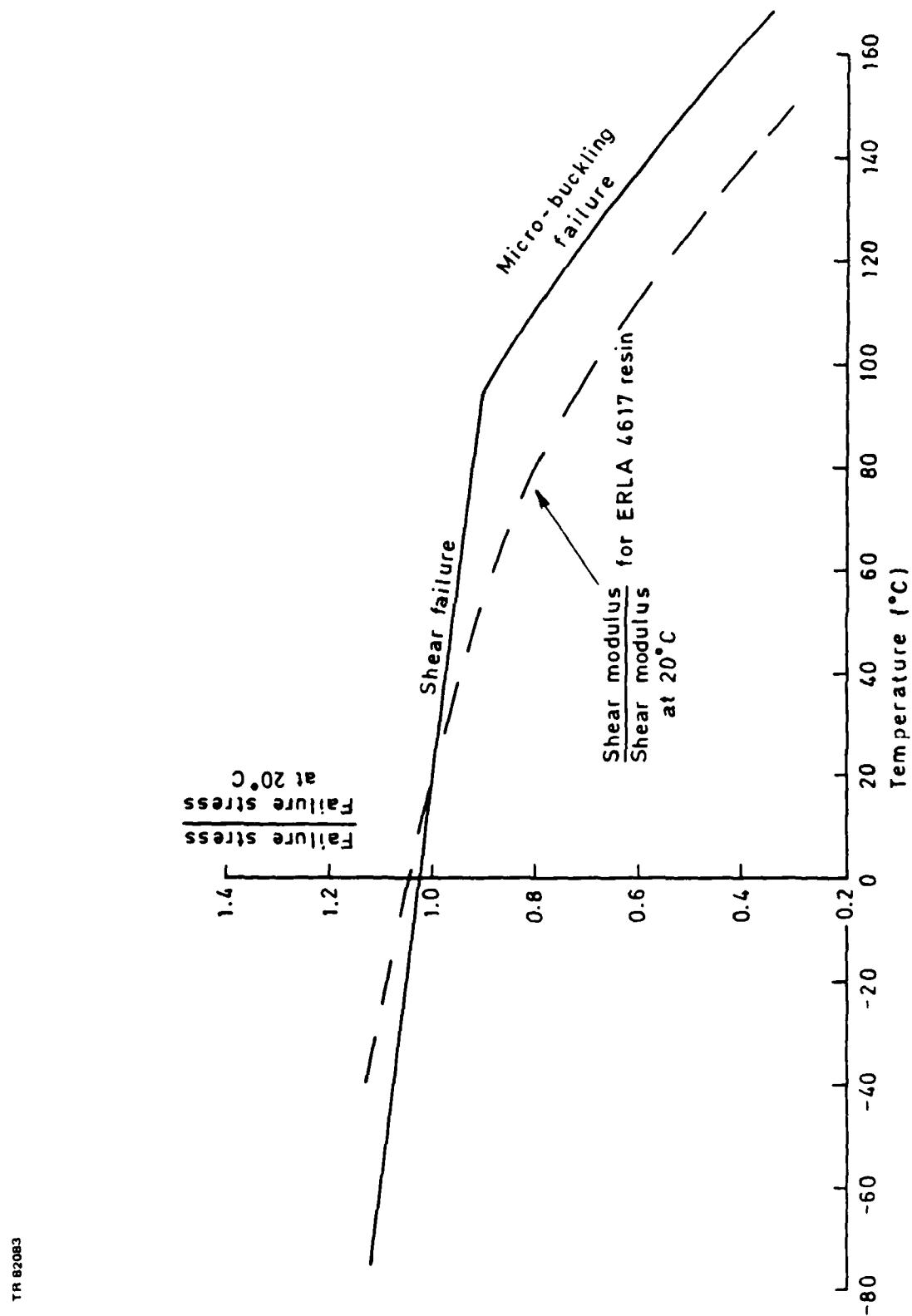


Fig 1 Variation of unidirectional compressive strength with temperature using standard RAE specimen (HTS ERLA 4617)

Fig 2



Fig 2 Failure surface of unidirectional compression specimen showing evidence of fibre micro-buckling

Fig 3

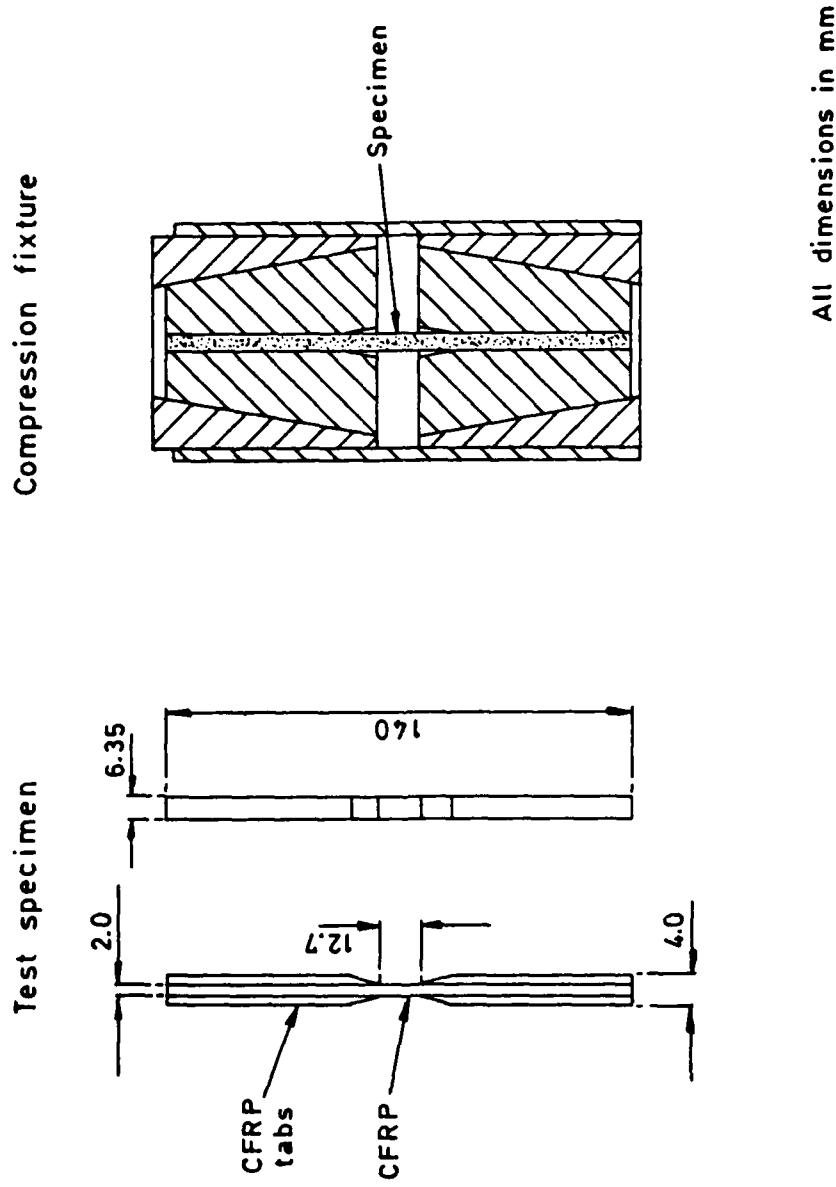


Fig 3 ASTM D3410 compression test specimen and fixture

Fig 4

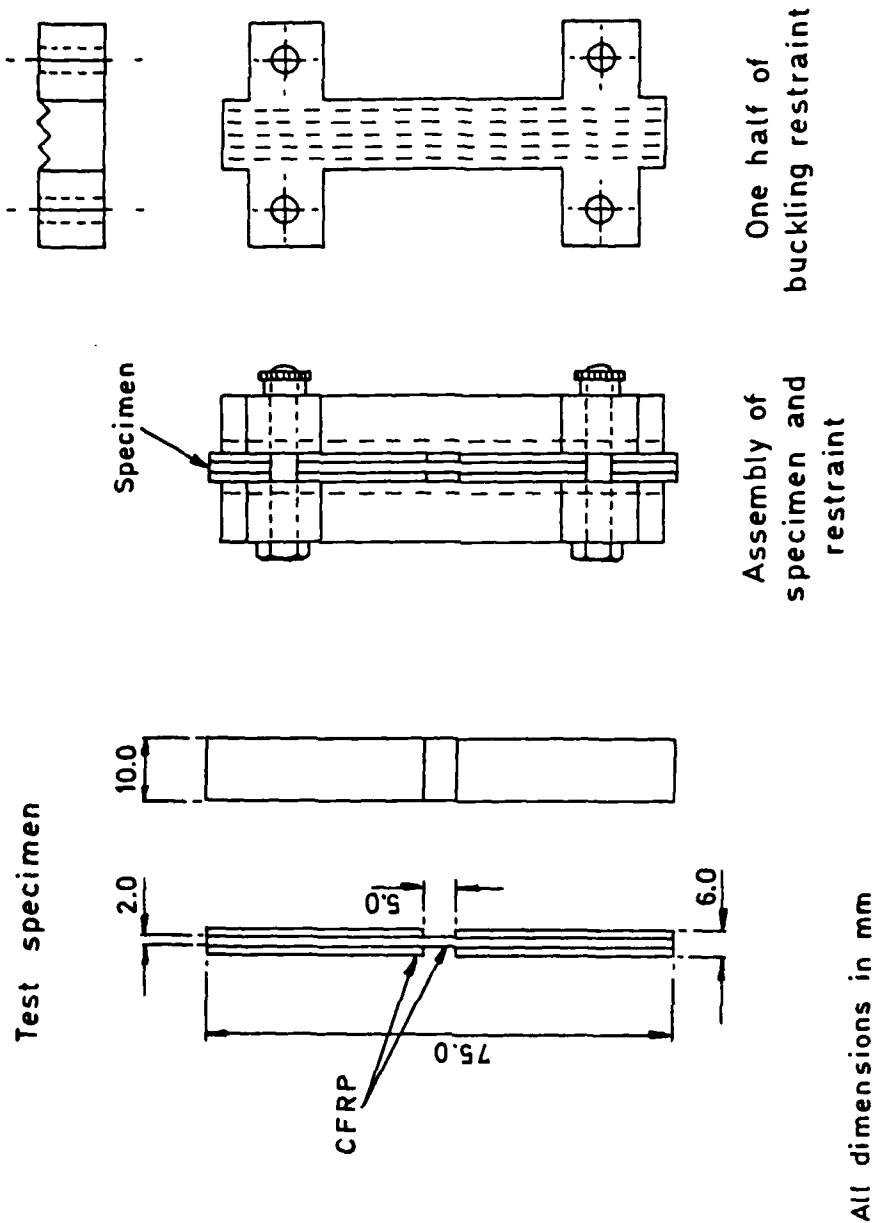


Fig 4 Modified ASTM D695 compression specimen and buckling restraint

Fig 5

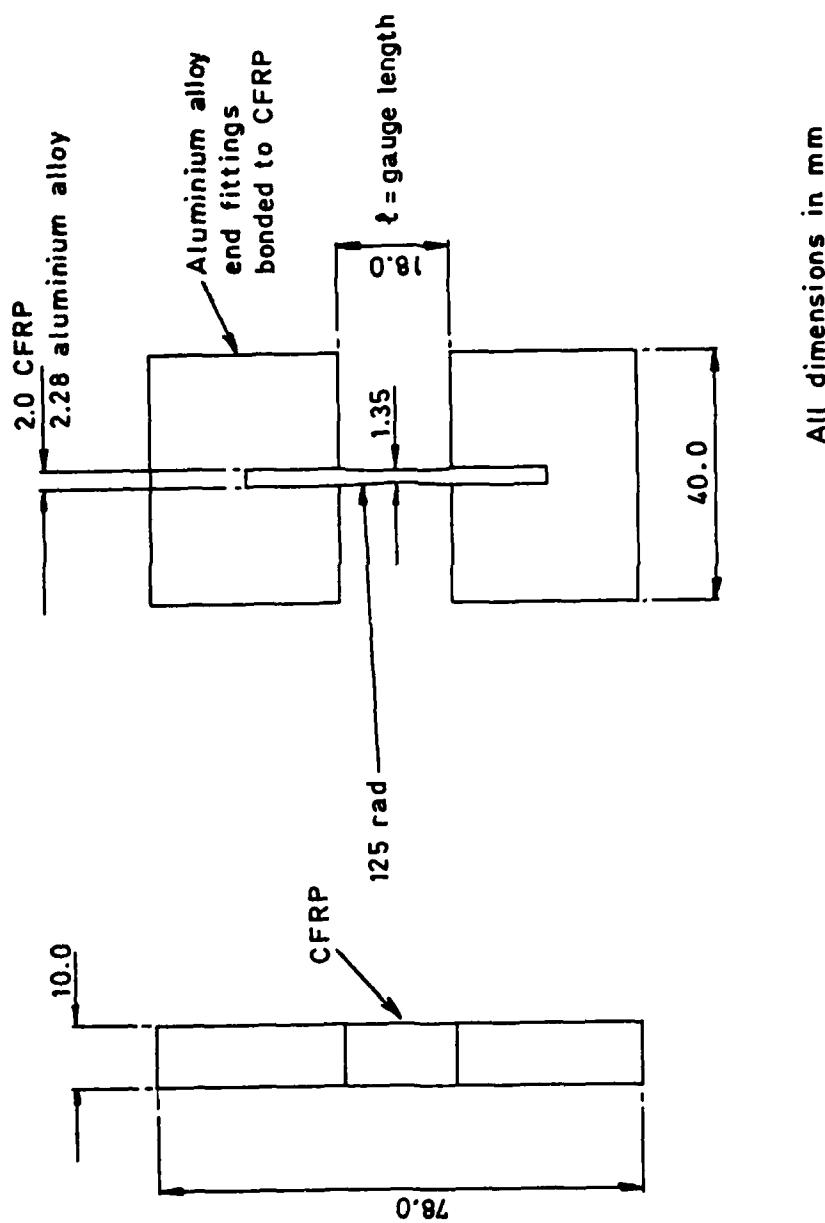


Fig 5 Standard RAE compressive strength specimen

Fig 6

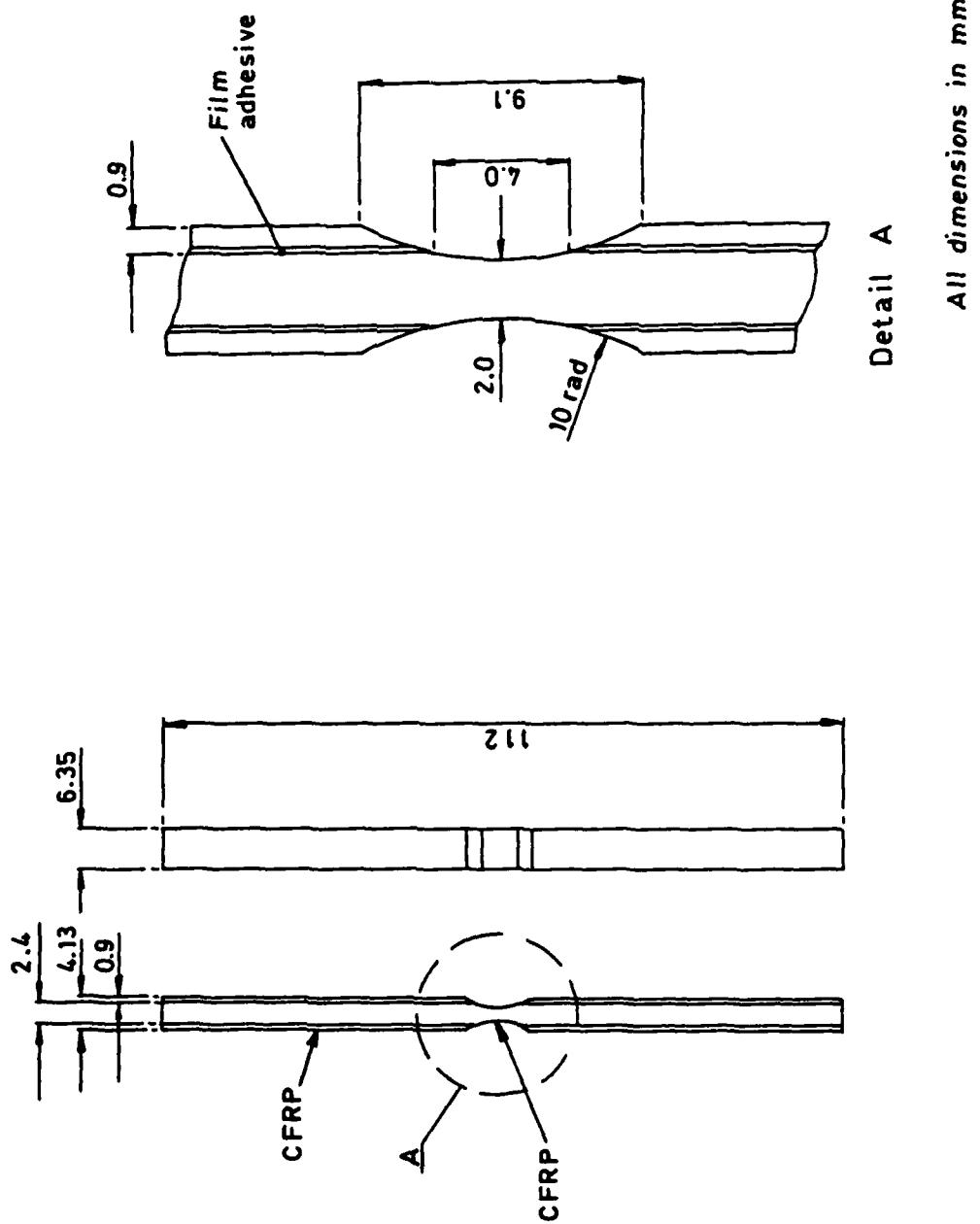


Fig 7

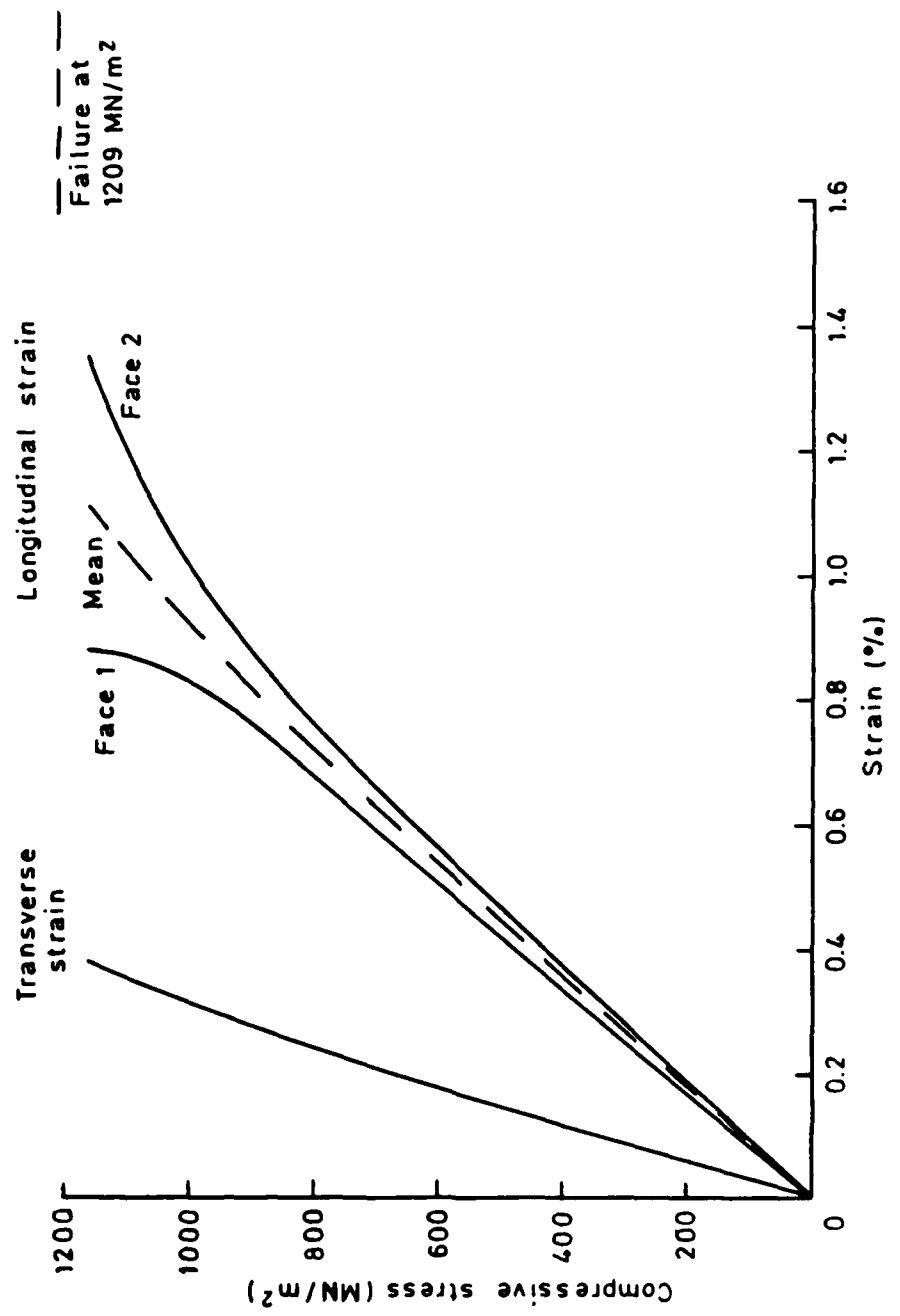


Fig 7 Stress-strain response for standard RAE unidirectional compression specimen (18 mm gauge length-waisted)

Fig 8

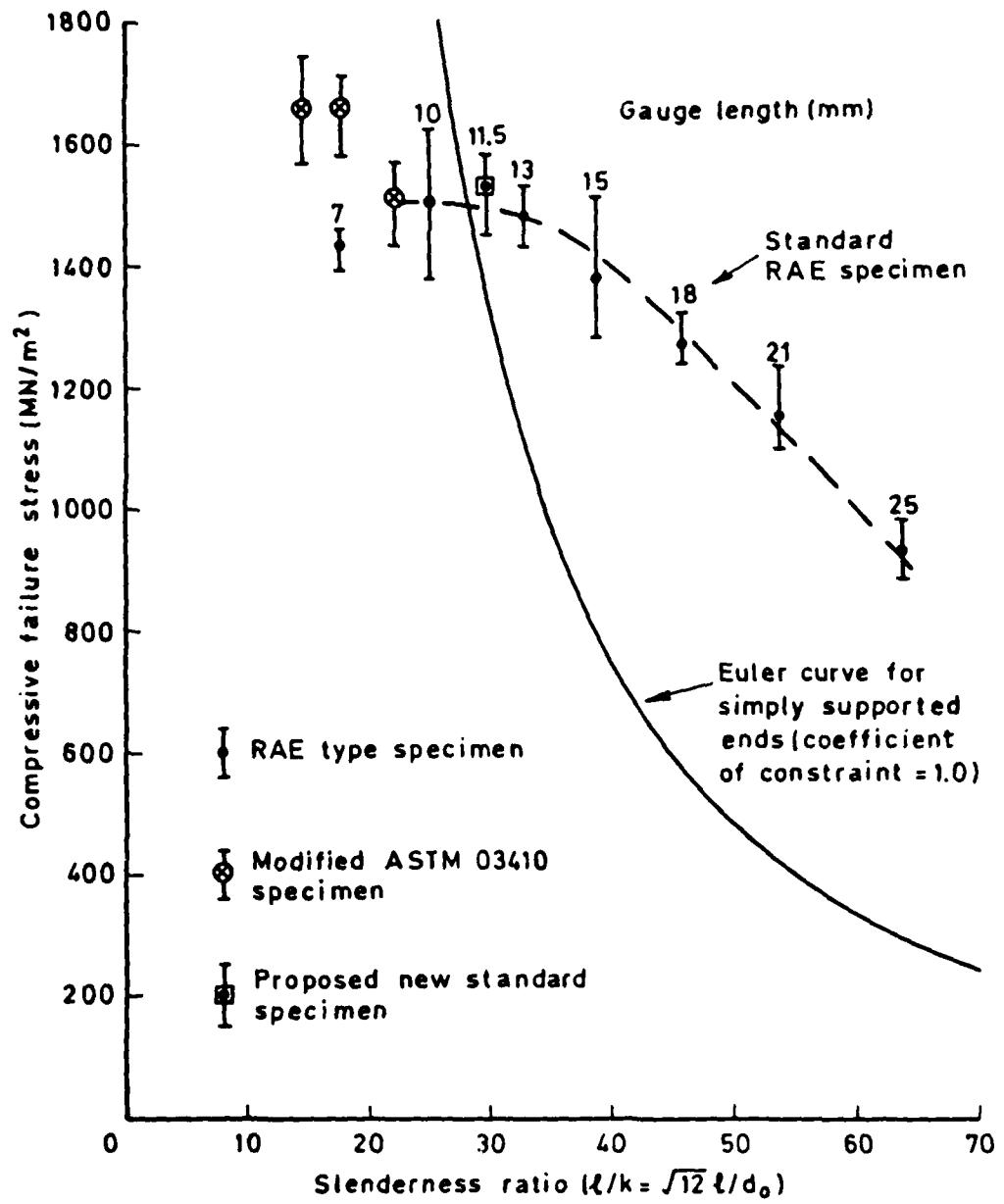


Fig 8 Variation of compressive strength with slenderness ratio for unidirectional CFRP

Fig 9

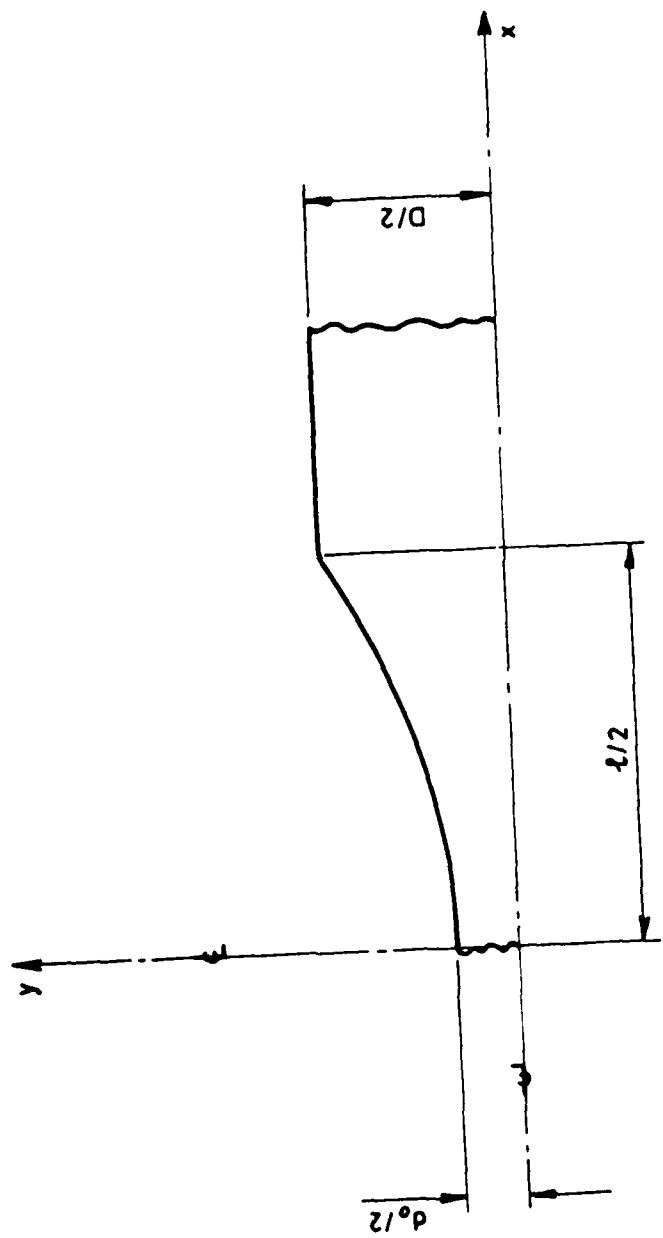


Fig 9 Part view of waisted gauge section

Fig 10a-d

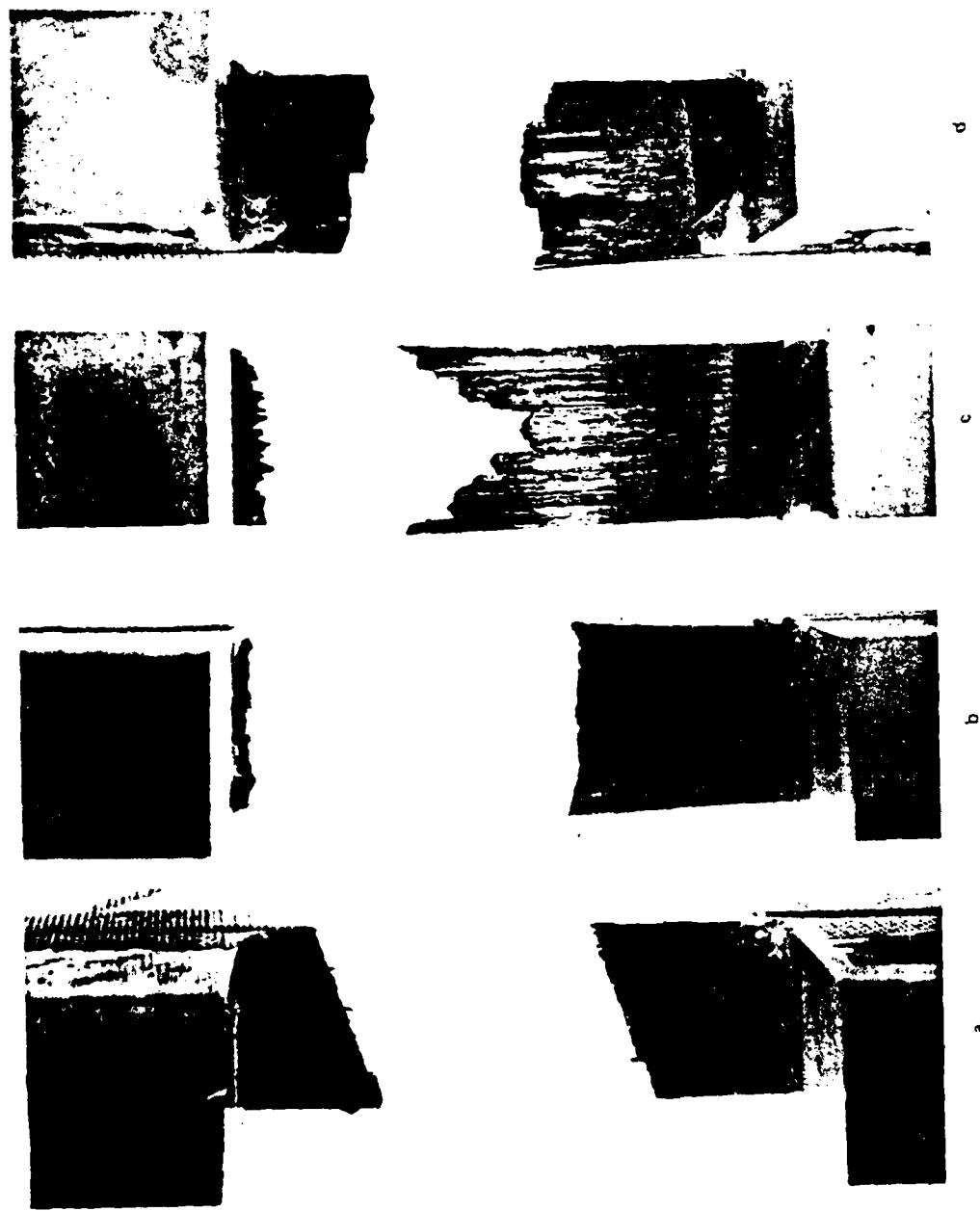


Fig 10a-d Overall failure modes of unidirectional compression specimens -- standard RAE type

Fig 11

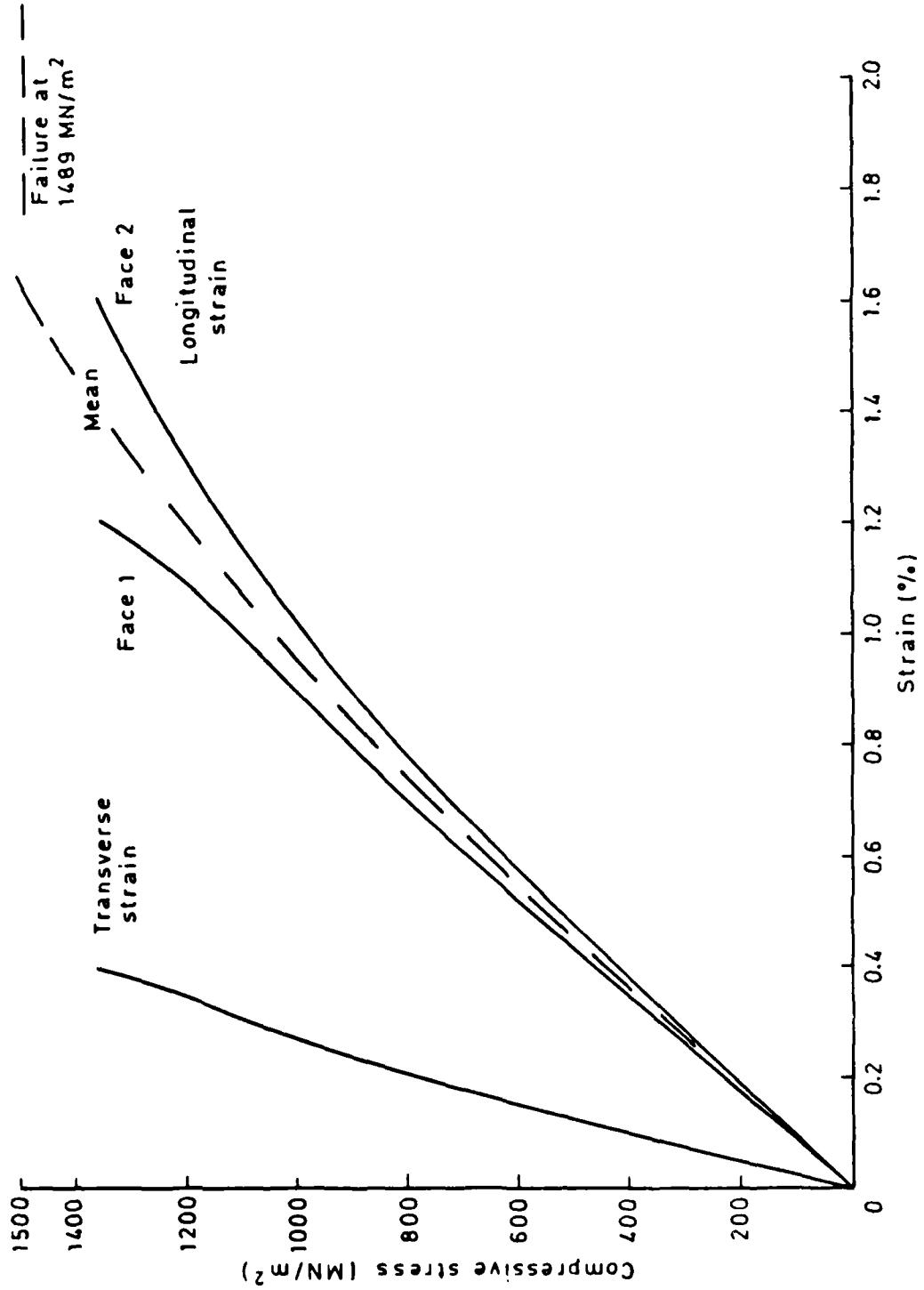


Fig 11 Stress-strain response for RAE type unidirectional compression specimen (13 mm gauge length-waisted)

Fig 12

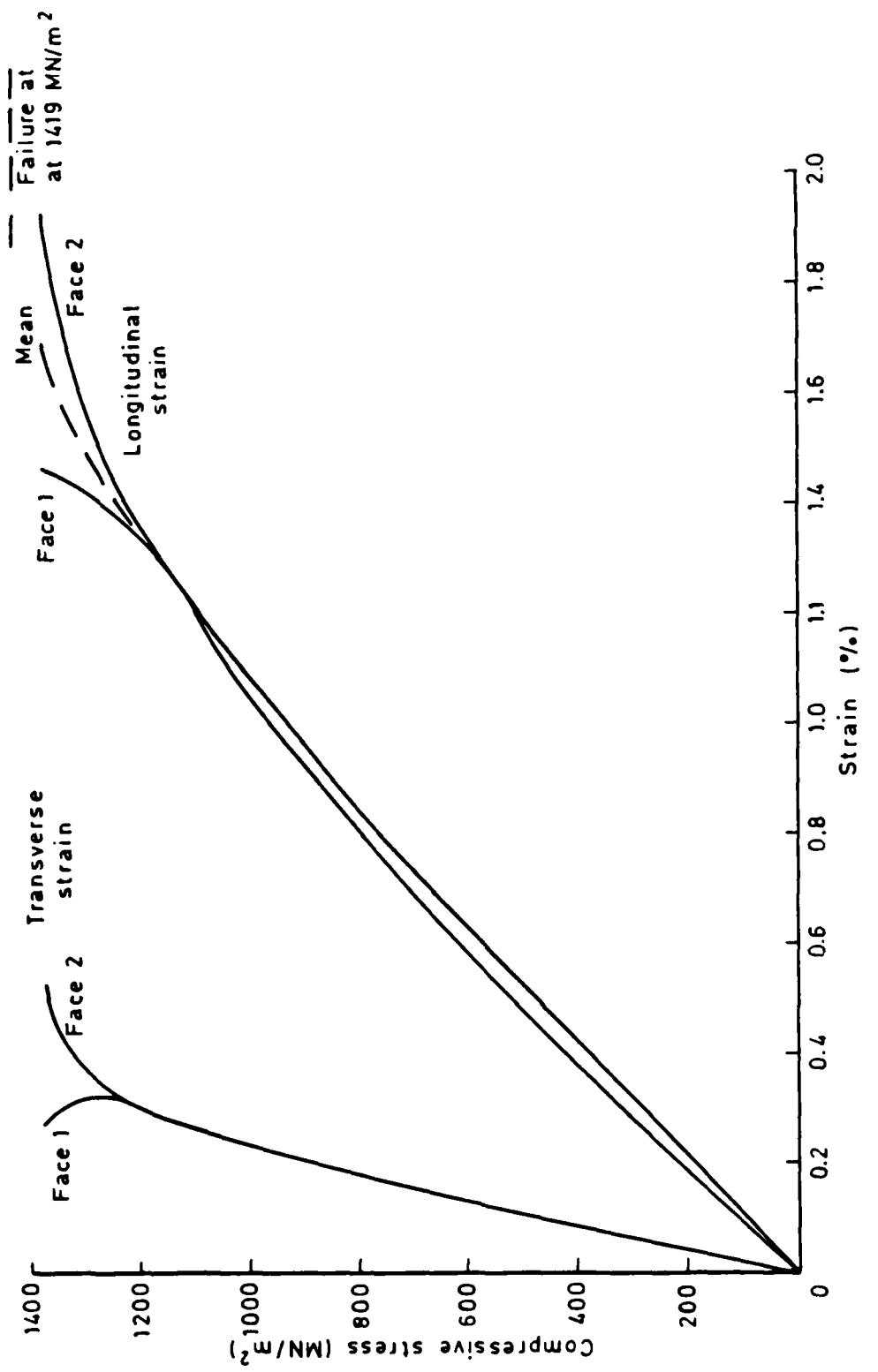


Fig 12 Stress-strain response for RAE type unidirectional compression specimen
(10 mm gauge length-waisted)

Fig 13

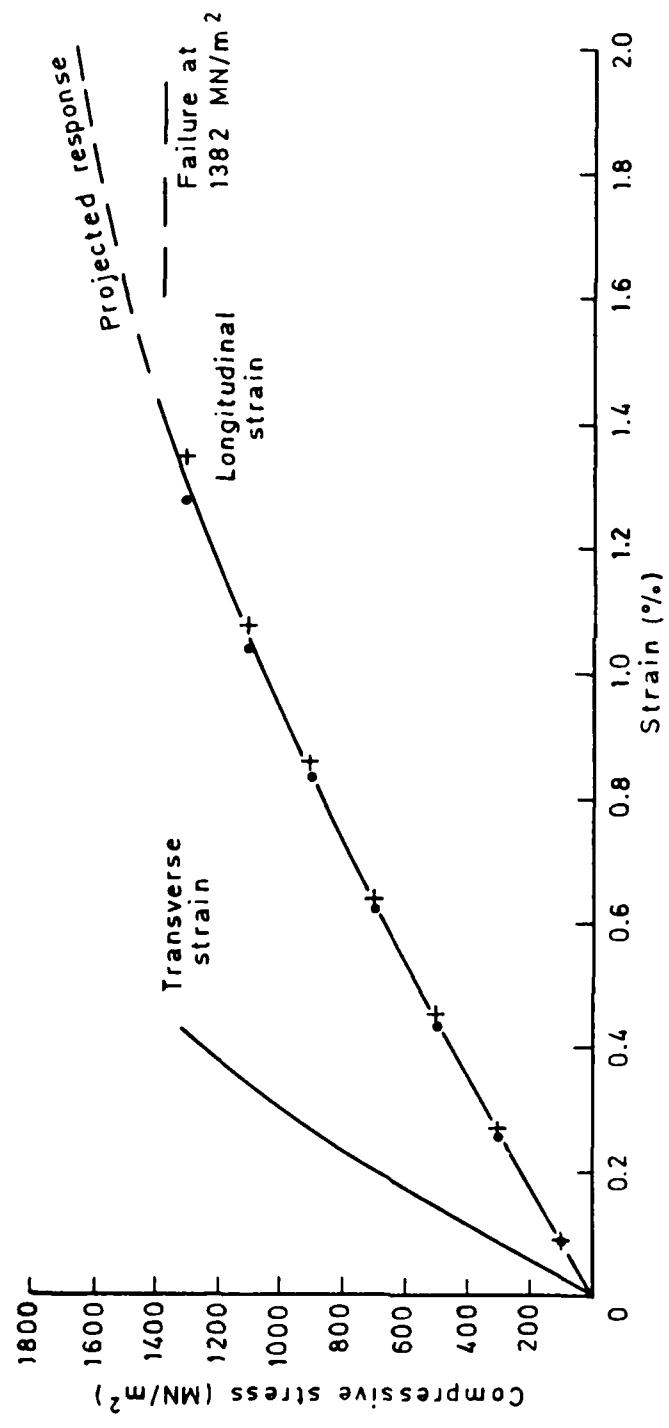


Fig 13 Stress-strain response for RAE type unidirectional compression specimen (13 mm gauge length plane)

Fig 14

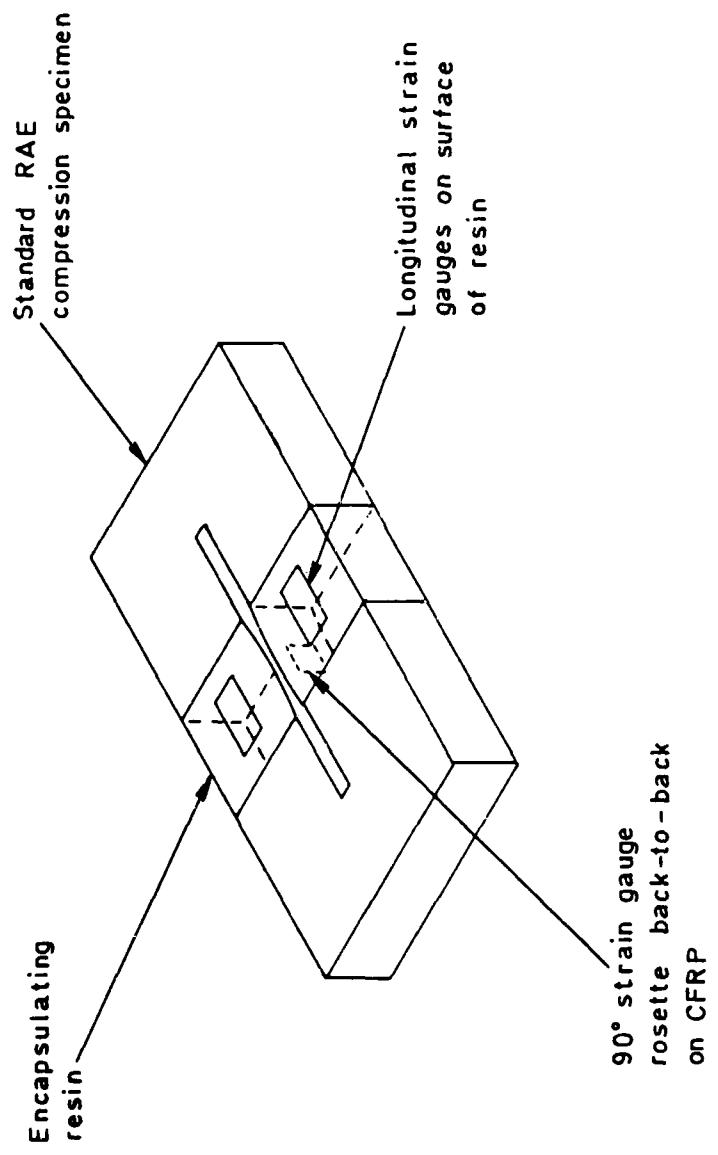


Fig 14 Stabilised RAE standard compression specimen (encapsulated)

Fig 15

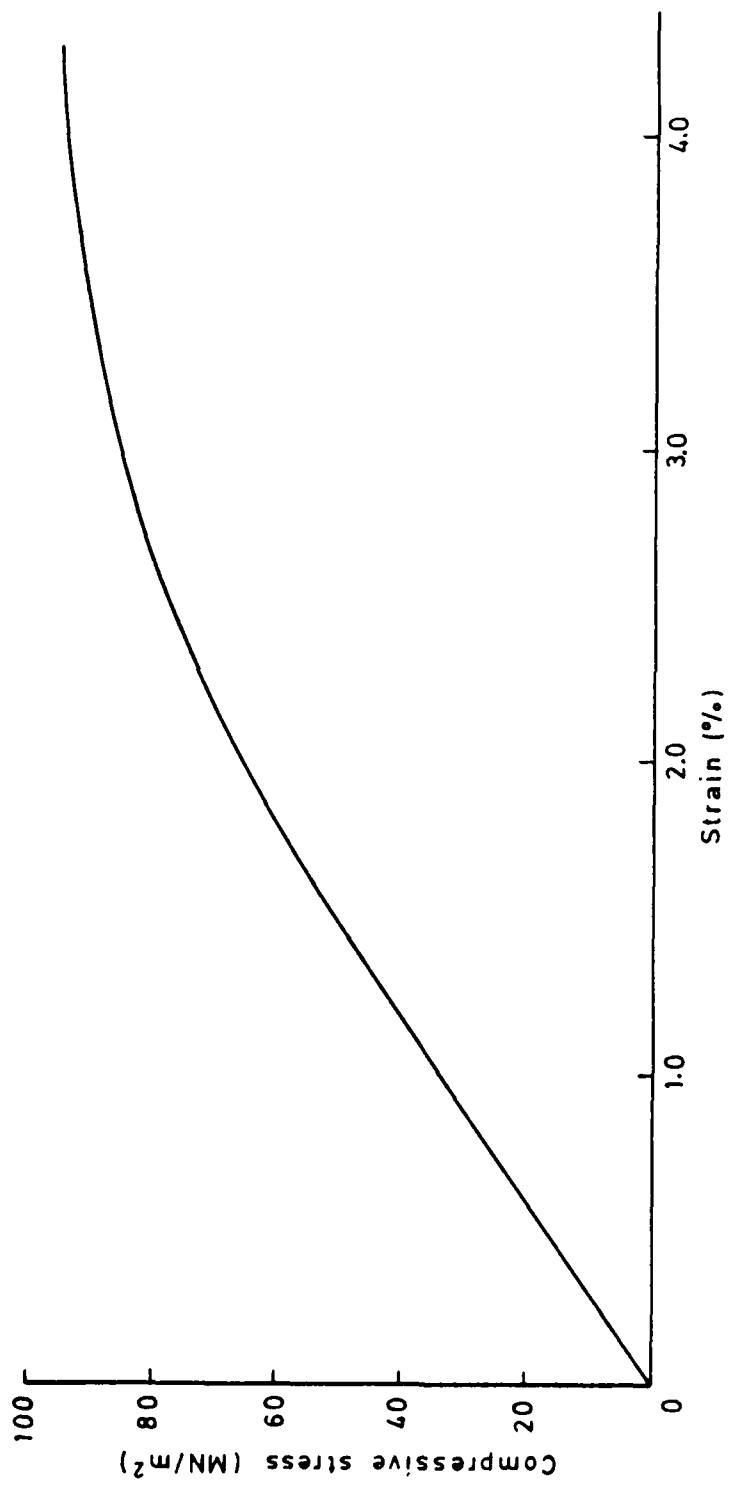


Fig 15 Stress-strain response for encapsulating resin (M/7753--HY956 Ciba-Geigy)

Fig 16

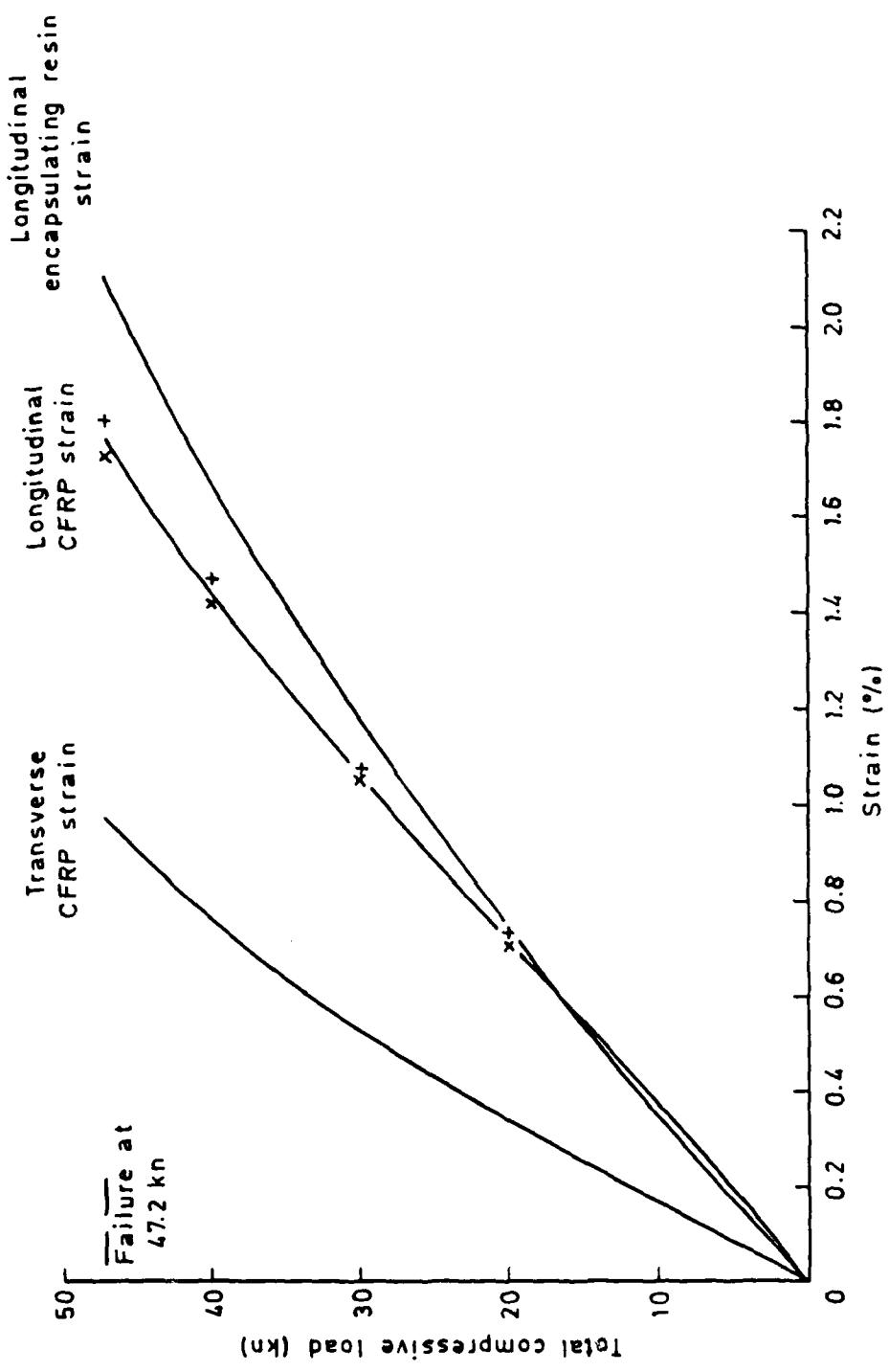


Fig 16 Load-strain response for encapsulated standard RAE unidirectional compression specimen

Fig 17

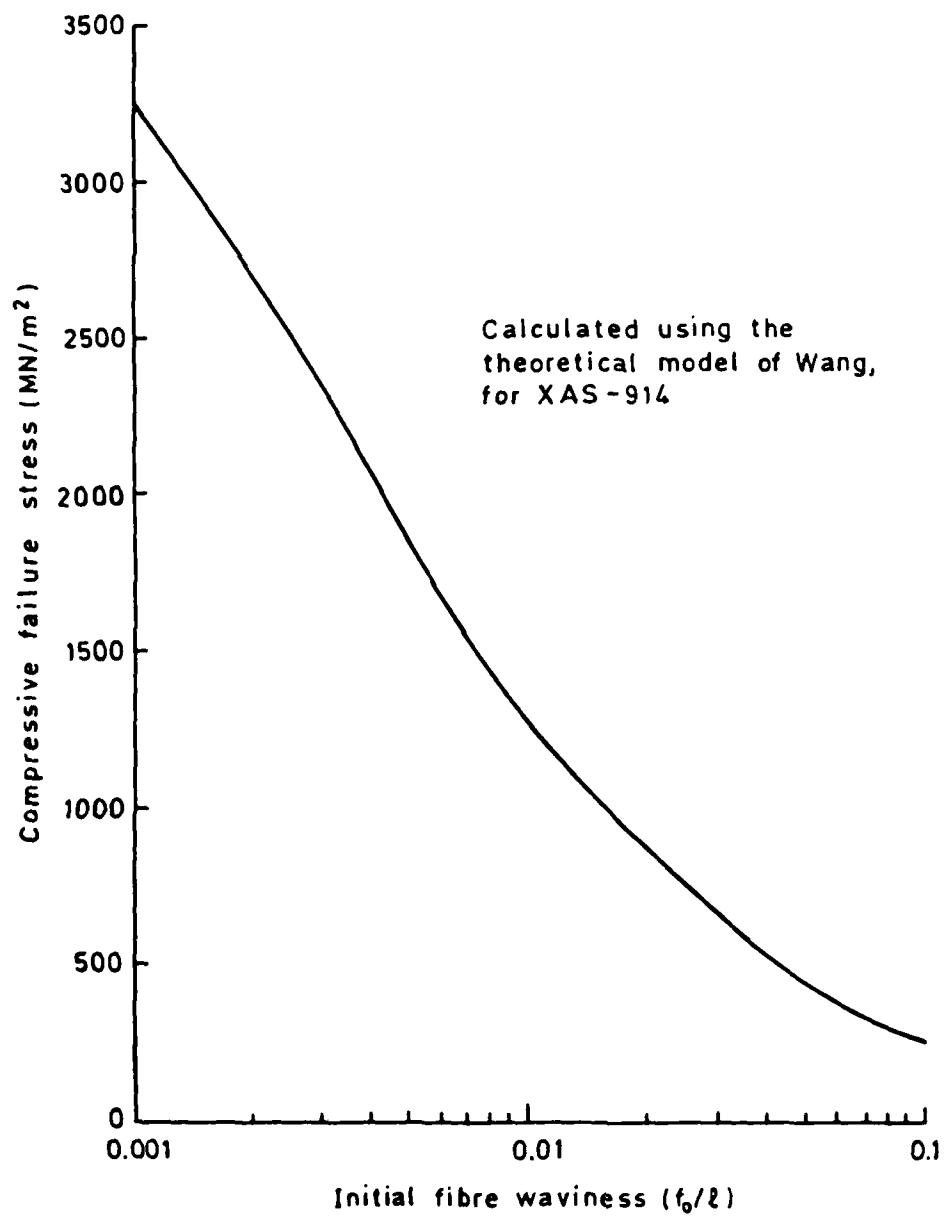


Fig 17 Theoretical variation of compressive strength with initial fibre waviness for unidirectional CFRP

Fig 18

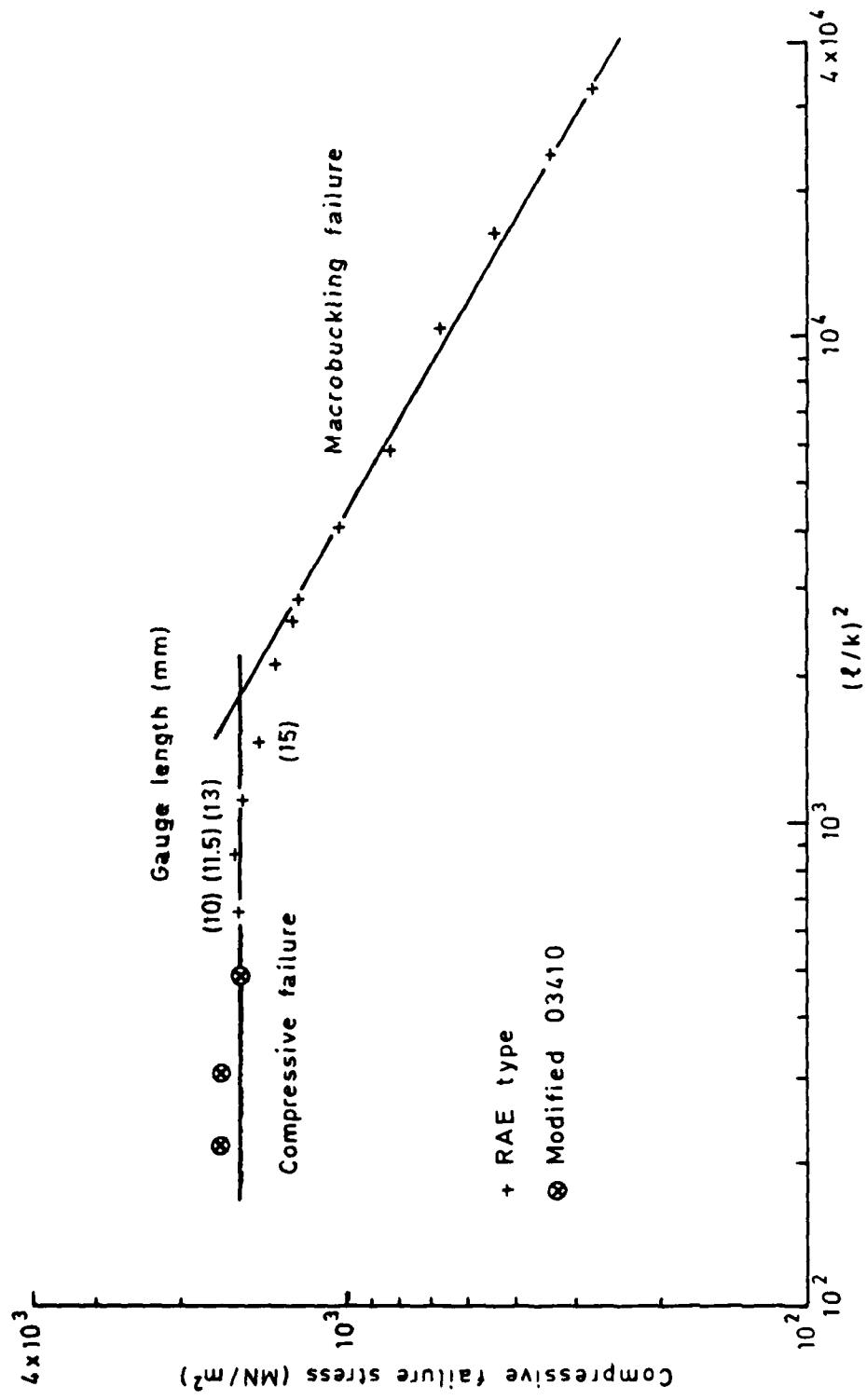


Fig 18 Logarithmic variation of compressive failure stress with the square of the slenderness ratio

Fig 19

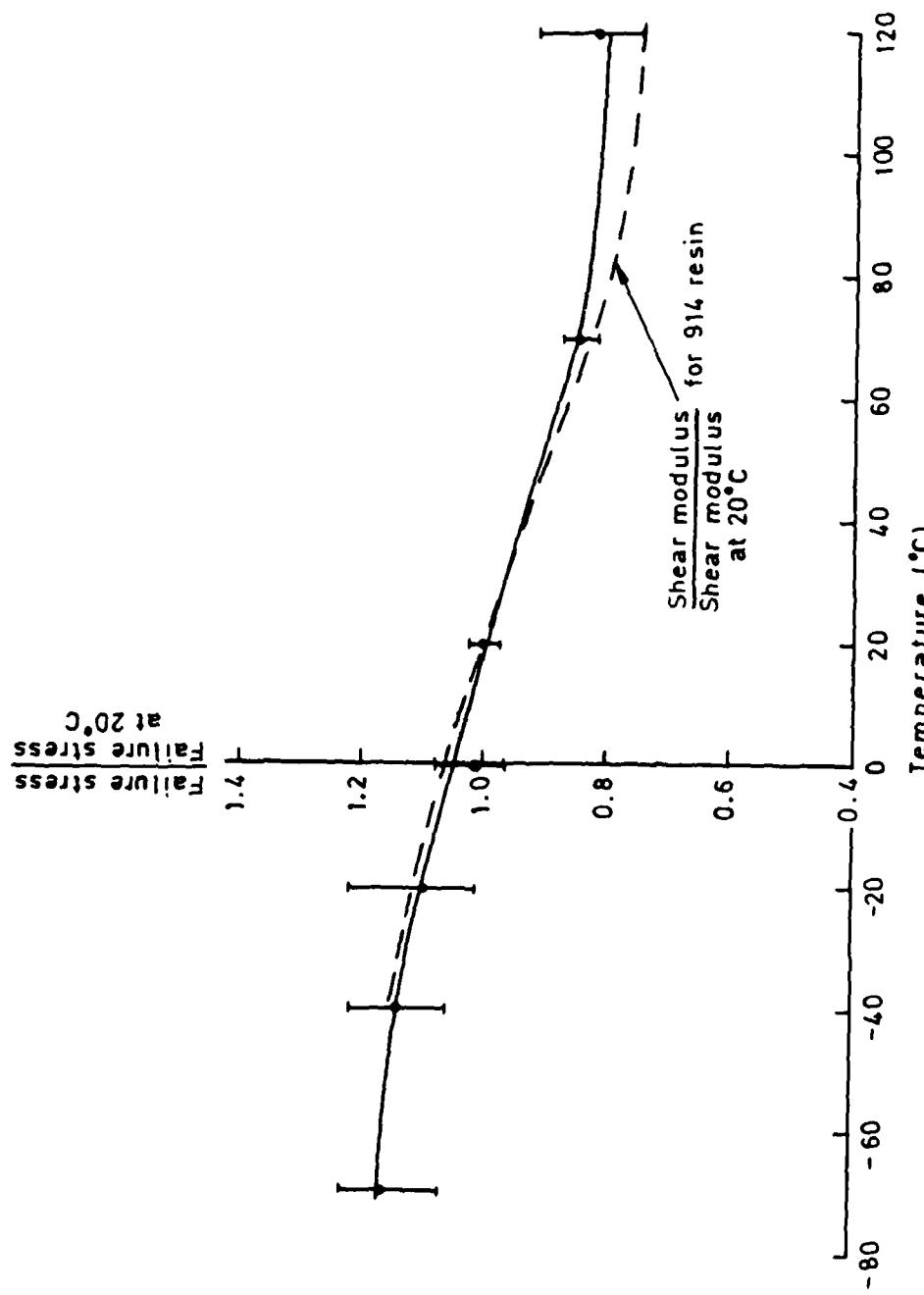


Fig 19 Variation of unidirectional compressive strength with temperature using standard RAE specimen (XAS-914)

Fig 20

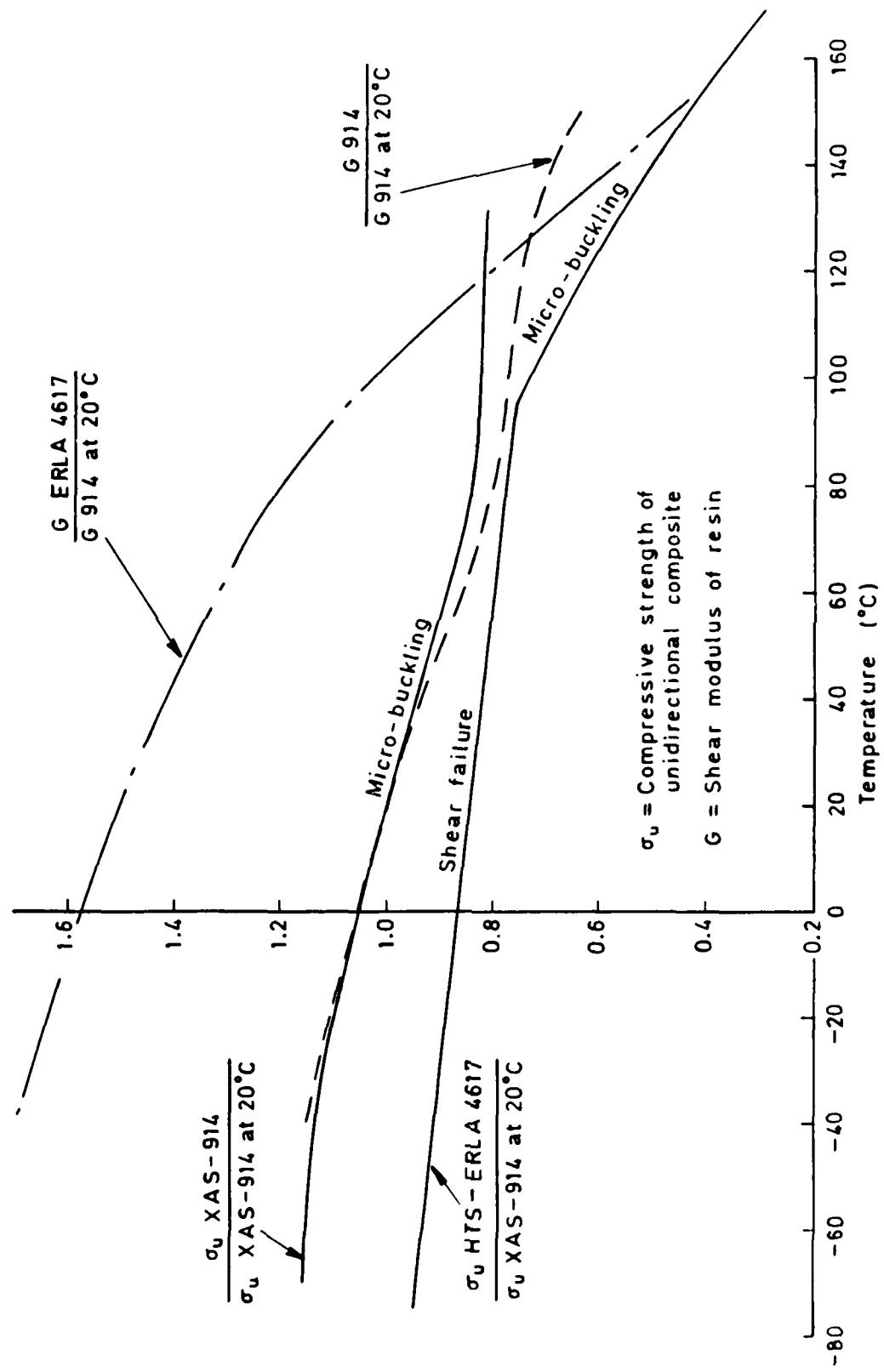


Fig 20 Comparison of compressive properties for XAS-914 and HTS ERLA4617 at various temperatures

Fig 21

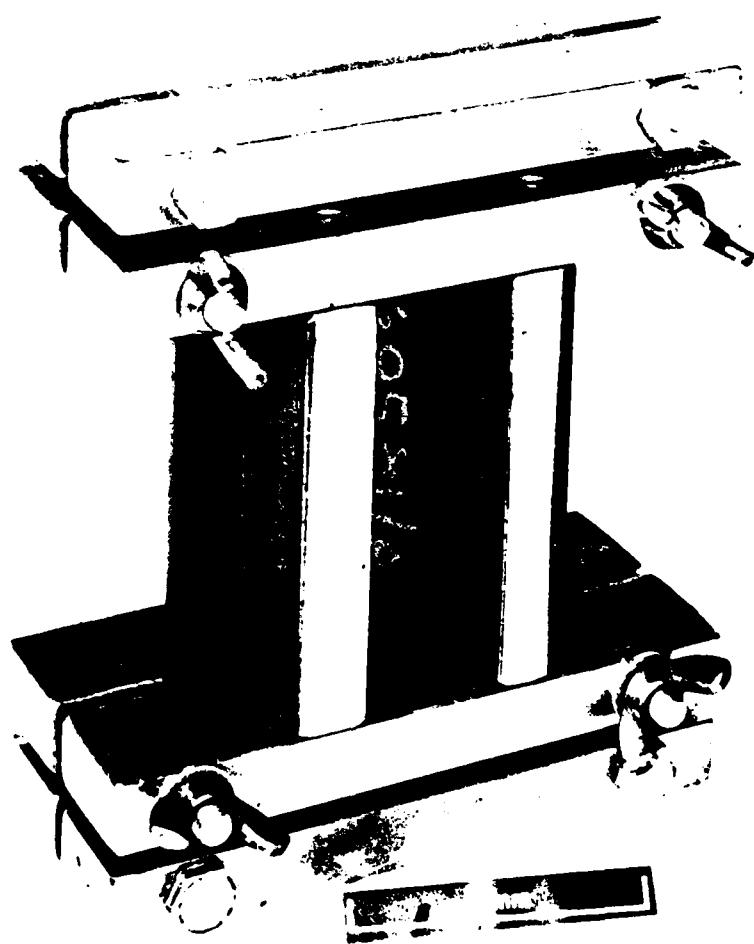


Fig 21 Compression test specimen and buckling restraint for multi-directional CFRP laminates

Fig 22

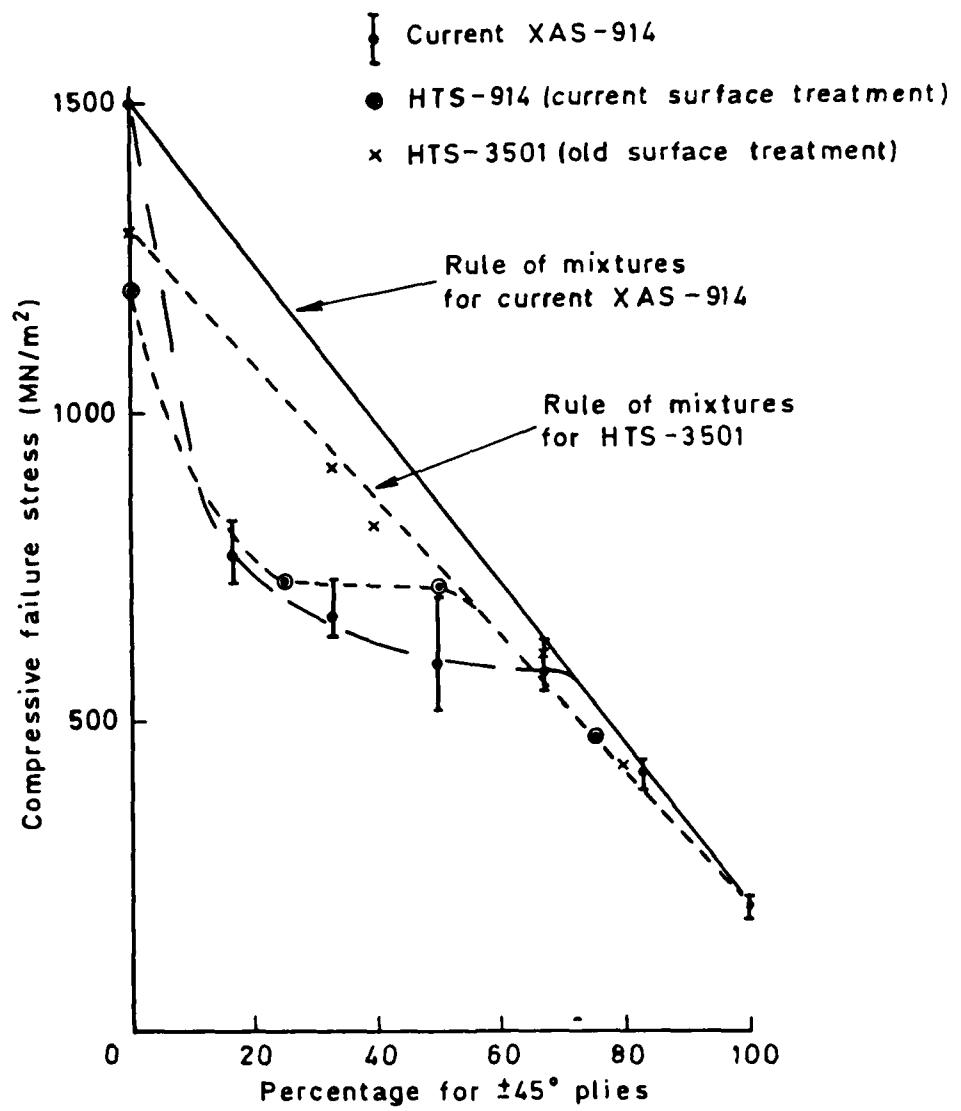


Fig 22 Variation of compressive strength with percentage of $\pm 45^\circ$ plies for $0^\circ \pm 45^\circ$ laminates

Fig 23

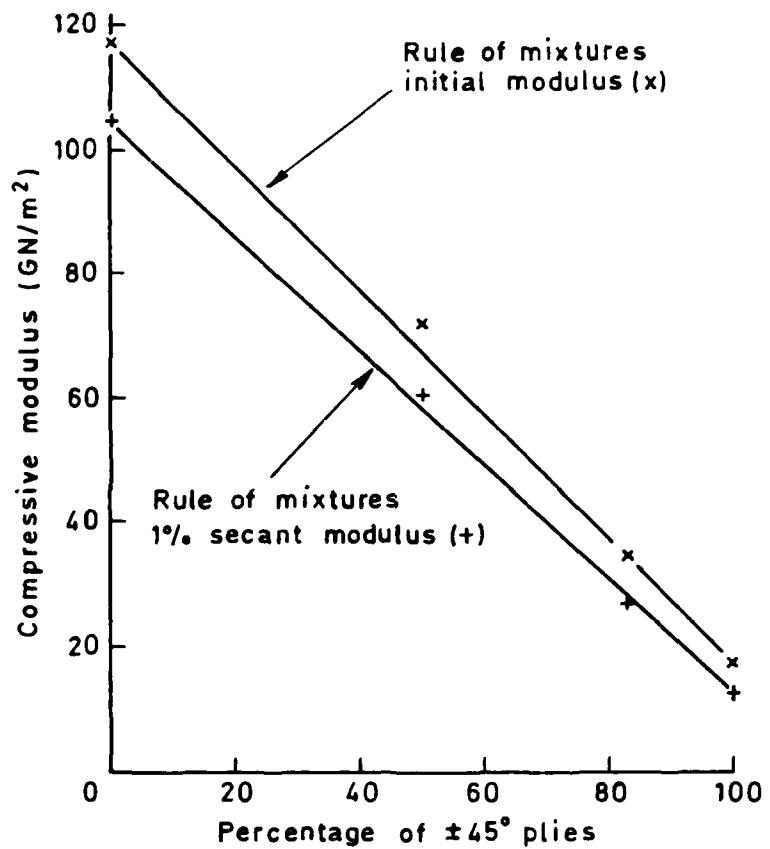


Fig 23 Variation of compressive modulus with percentage of $\pm 45^\circ$ plies for $0^\circ \pm 45^\circ$ laminates

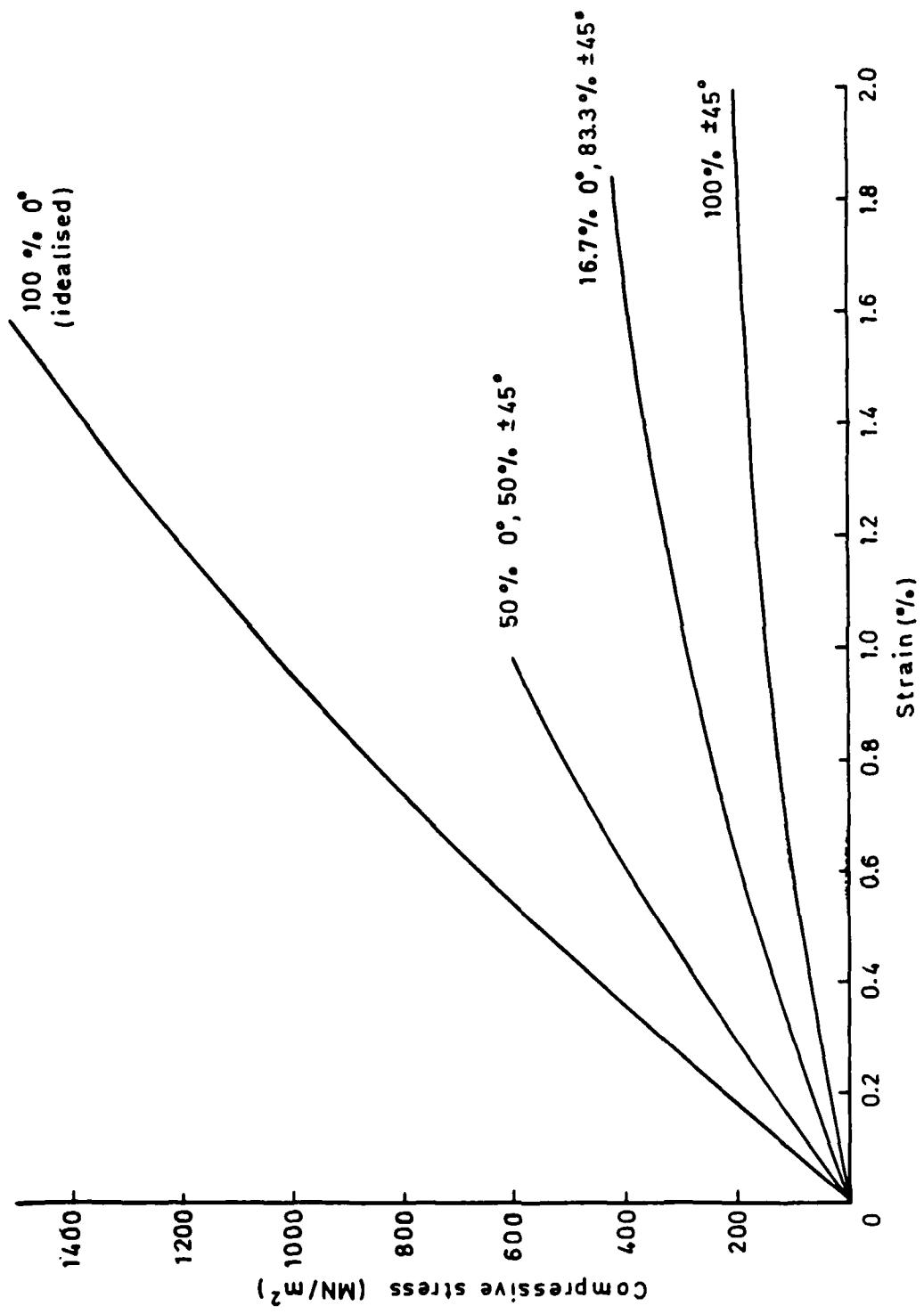


Fig 24 Compressive stress-strain response for various CFRP laminates

REPORT DOCUMENTATION PAGE

Overall security classification of this page

UNLIMITED

As far as possible this page should contain only unclassified information. If it is necessary to enter classified information, the box above must be marked to indicate the classification, e.g. Restricted, Confidential or Secret.

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17. Abstract			
<p>The merits of some alternative test specimens for the measurement of compressive strength of unidirectional carbon fibre reinforced plastics are discussed and it is concluded that whilst the RAE design is close to the optimum some improvements are desirable. Tests demonstrated that the present standard RAE specimen is measuring a buckling strength. A modified specimen is suggested which measures a representative compressive strength of the material and this value has been shown to be attainable in the 0° plies of some multi-directional laminates.</p> <p>It has been shown that compressive failure of current unidirectional material involves fibre microbuckling and that a shear failure is not attainable even at -70°C. The compressive stress-strain response of unidirectional CFRP has been shown to be nonlinear and this is attributed to elastic fibre microinstability which is initiated by initial fibre waviness.</p> <p>A brief experimental investigation has shown that the compressive strength of multidirectional CFRP (0° ± 45°) does not obey a rule of mixtures when more than 30% 0° plies are present. It is therefore concluded that the compressive strengths of such laminates cannot be calculated from the properties of the constituent plies.</p> <p style="text-align: right;">X C.U.</p>			

